



*The Proceedings*  
OF  
THE INSTITUTION OF  
ELECTRICAL ENGINEERS

FOUNDED 1871: INCORPORATED BY ROYAL CHARTER 1921

PART A  
POWER ENGINEERING

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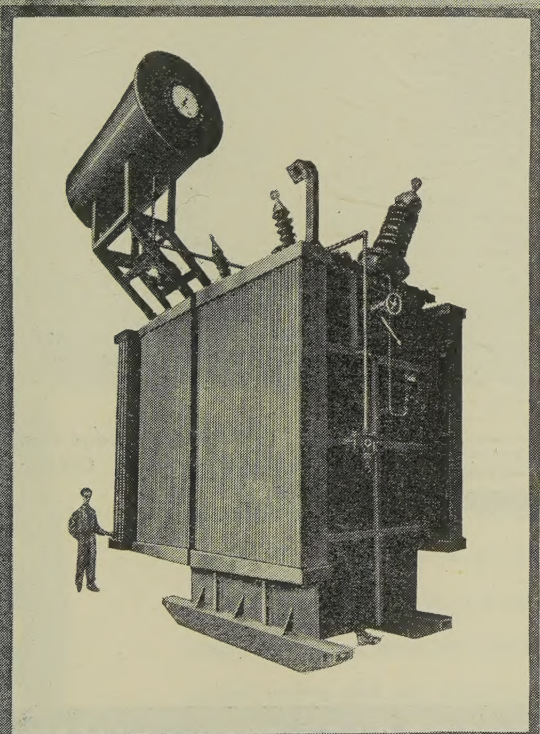
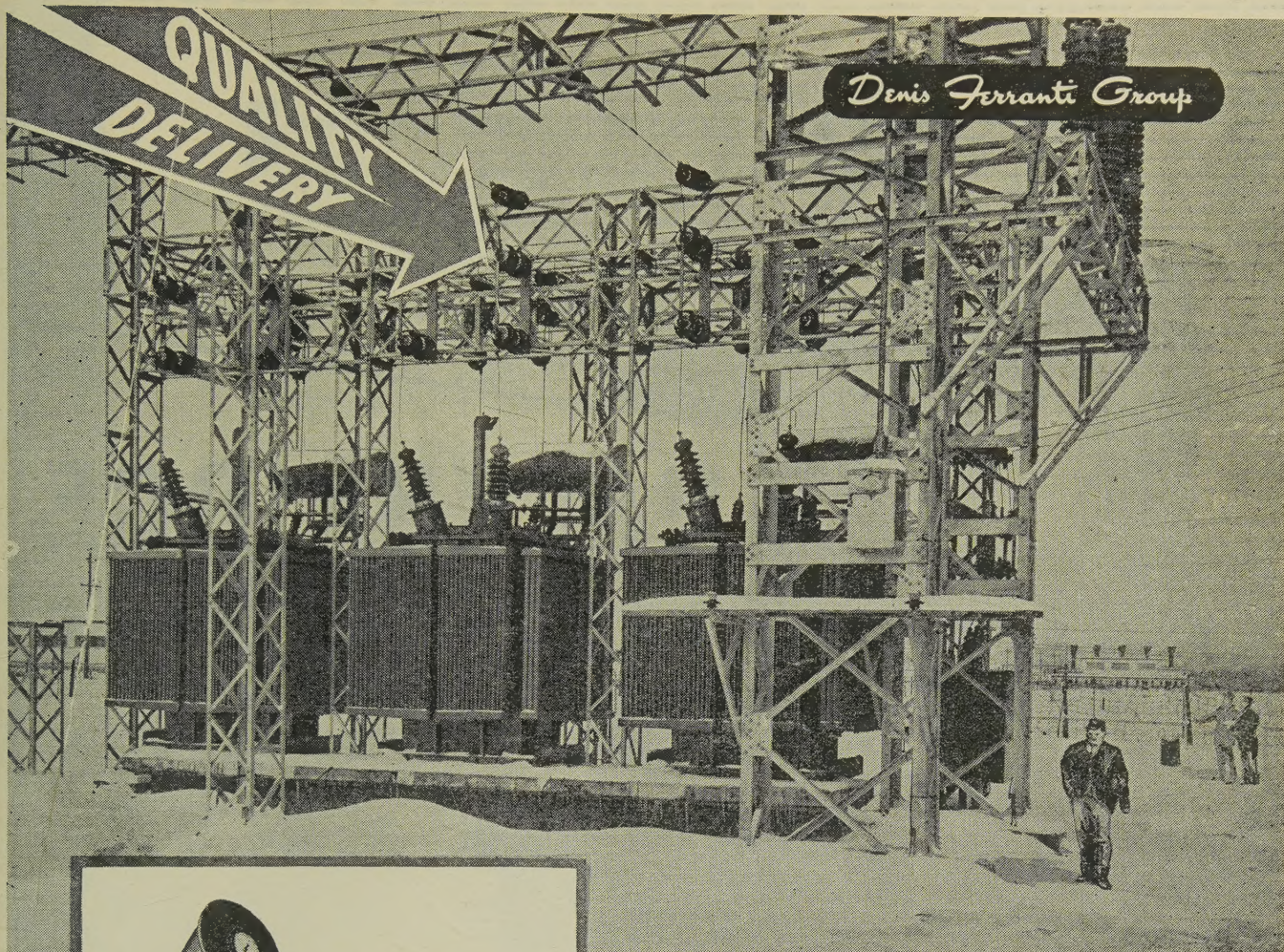
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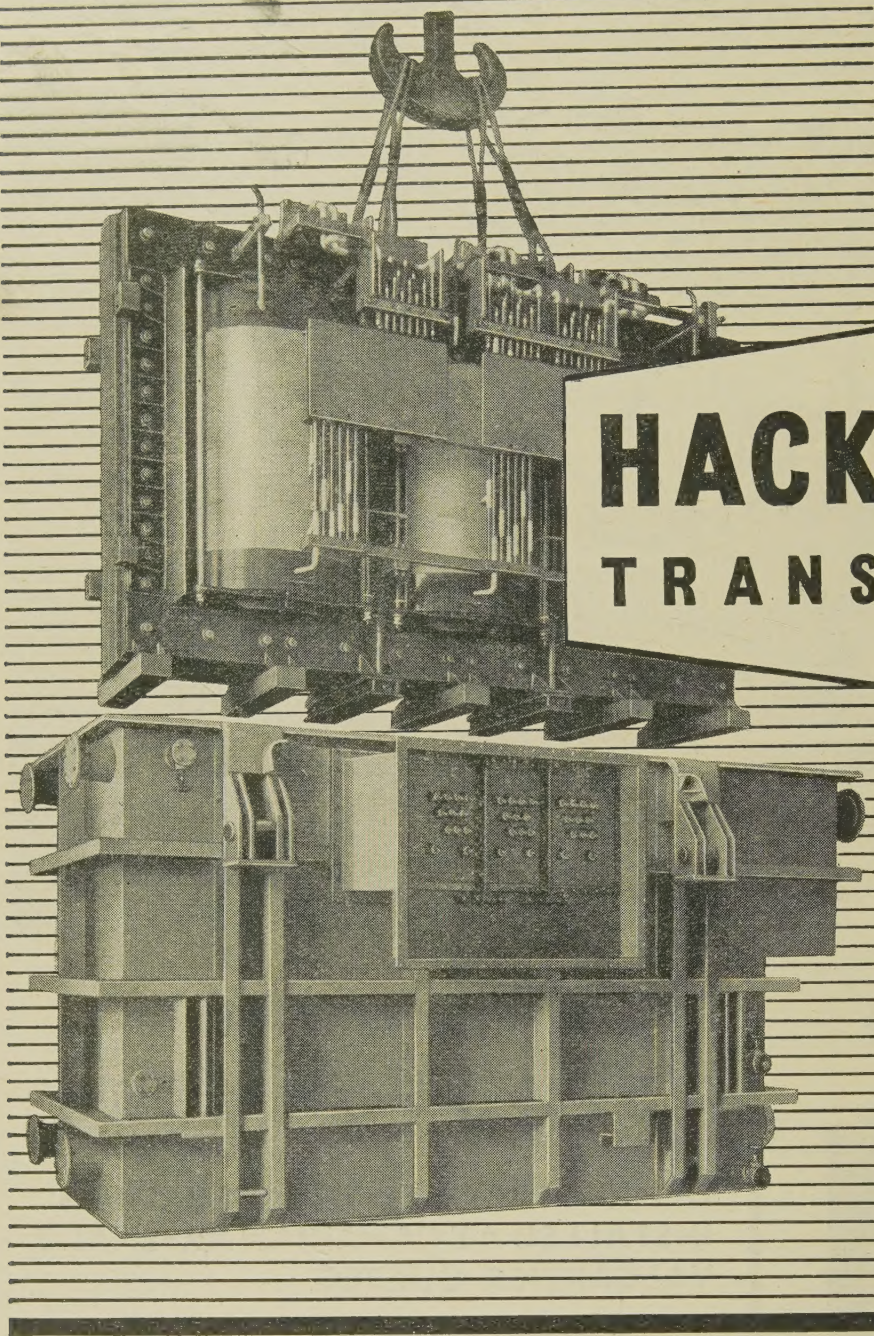
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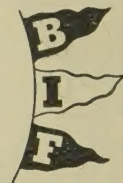
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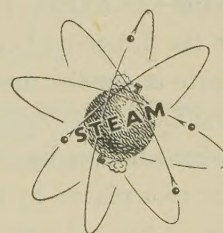
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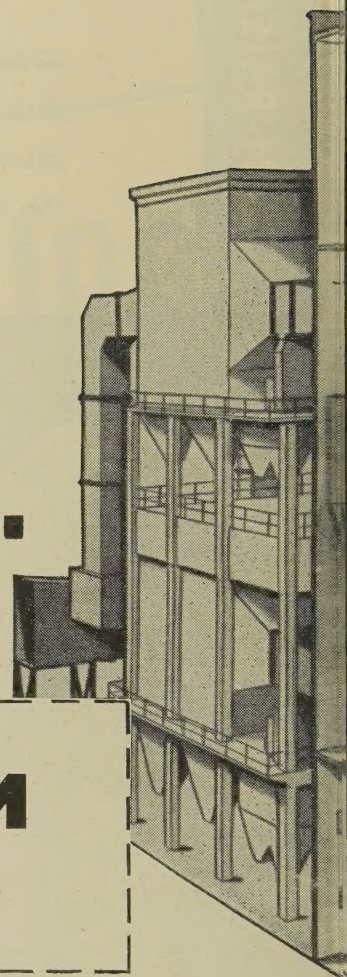
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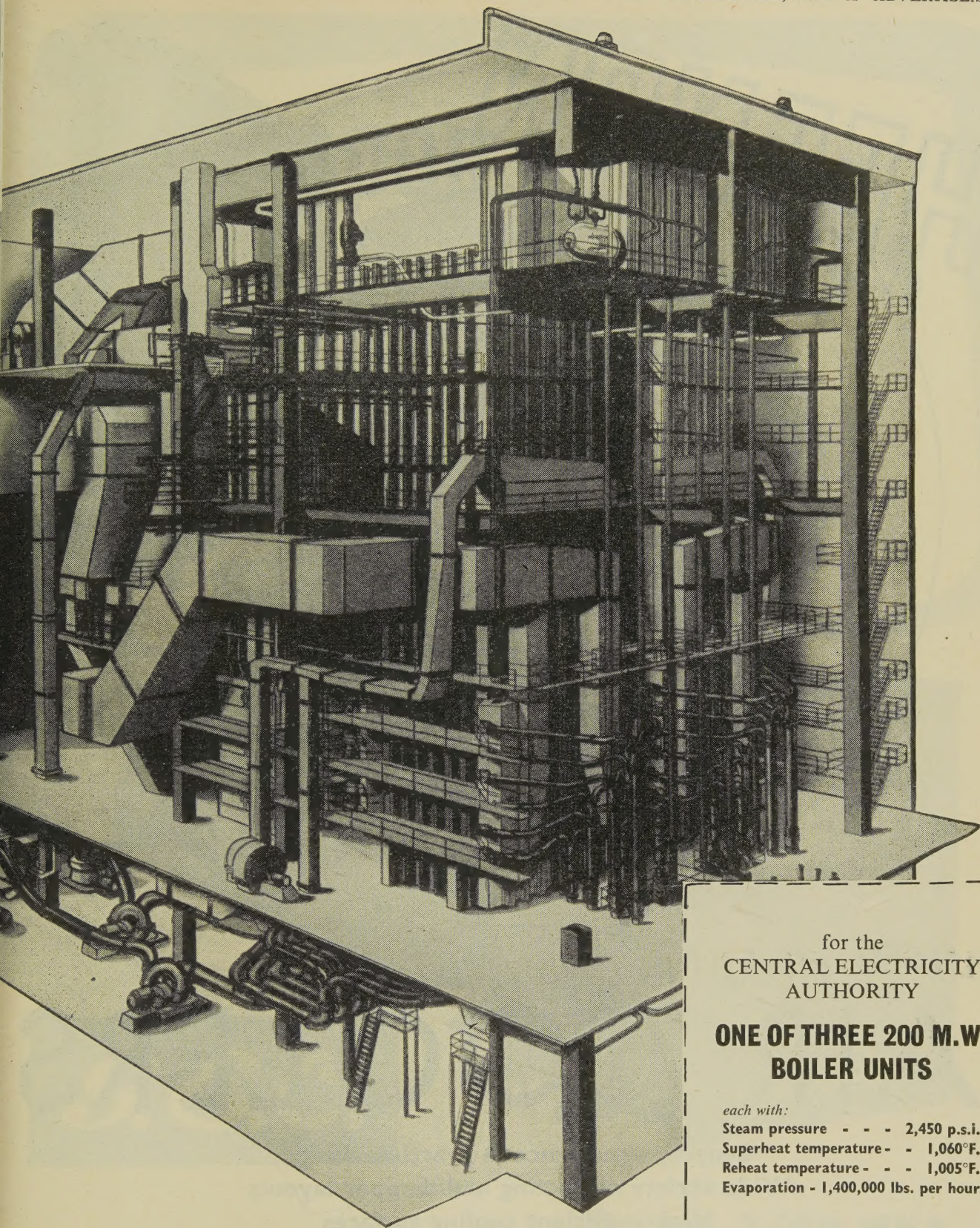
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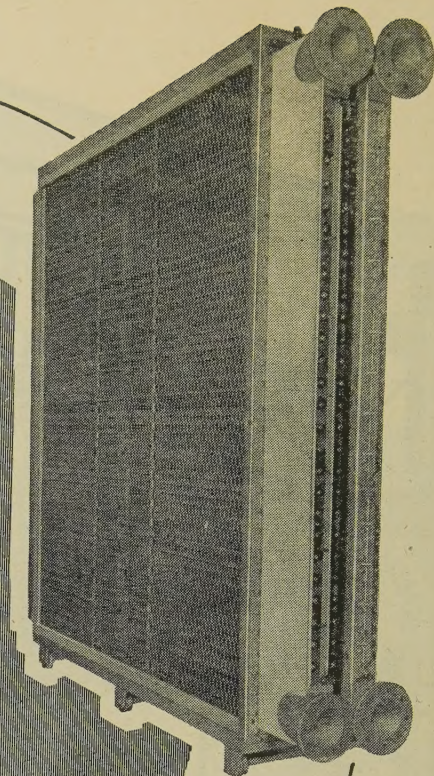
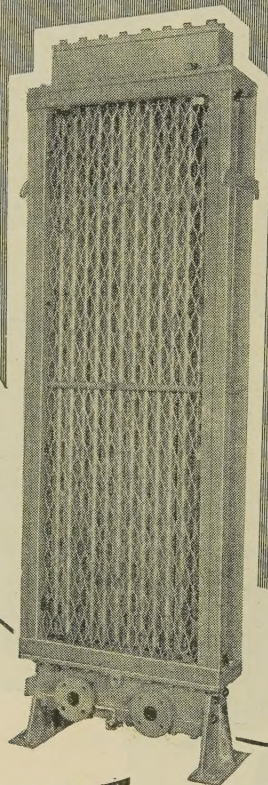
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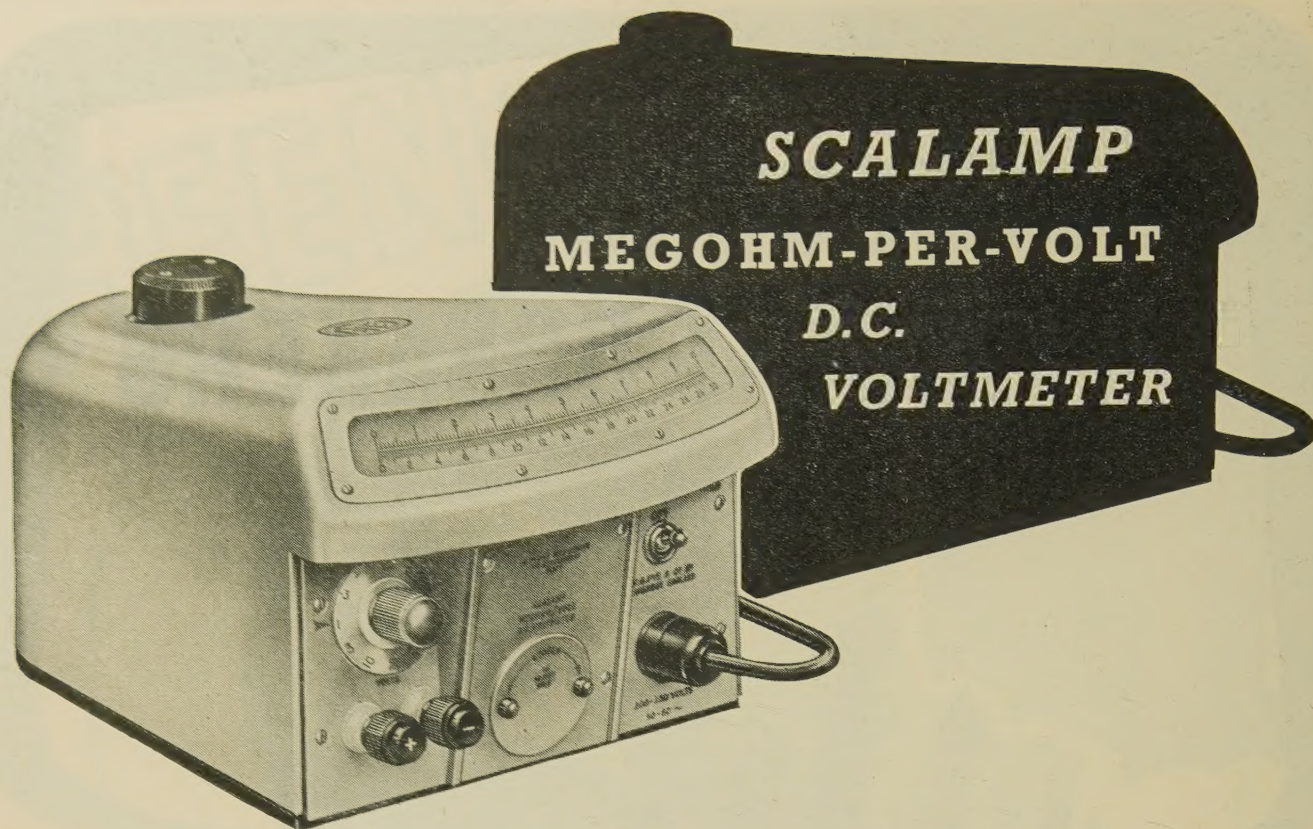
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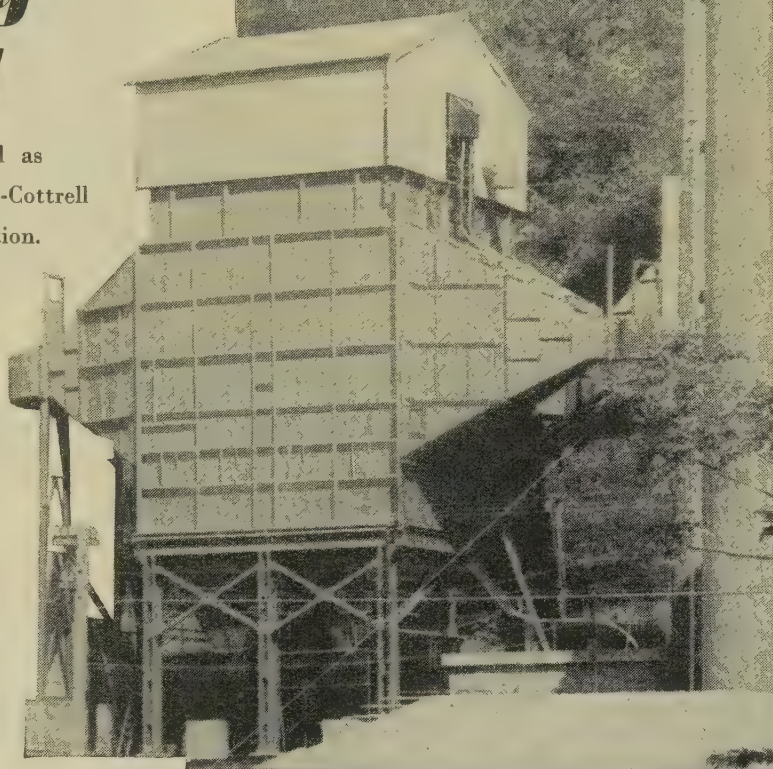
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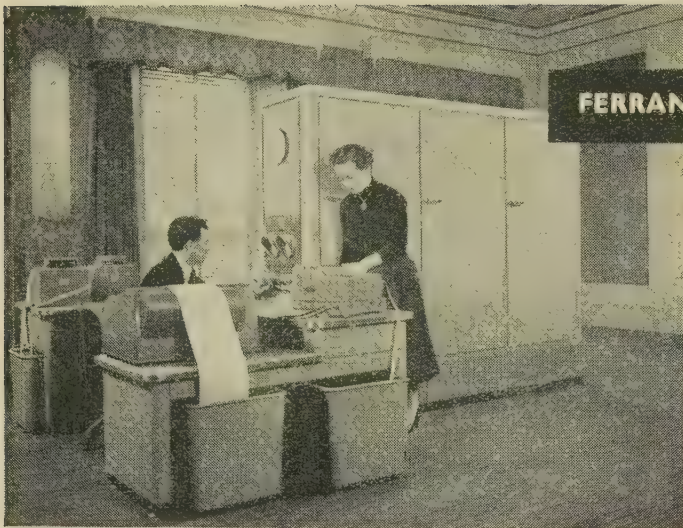
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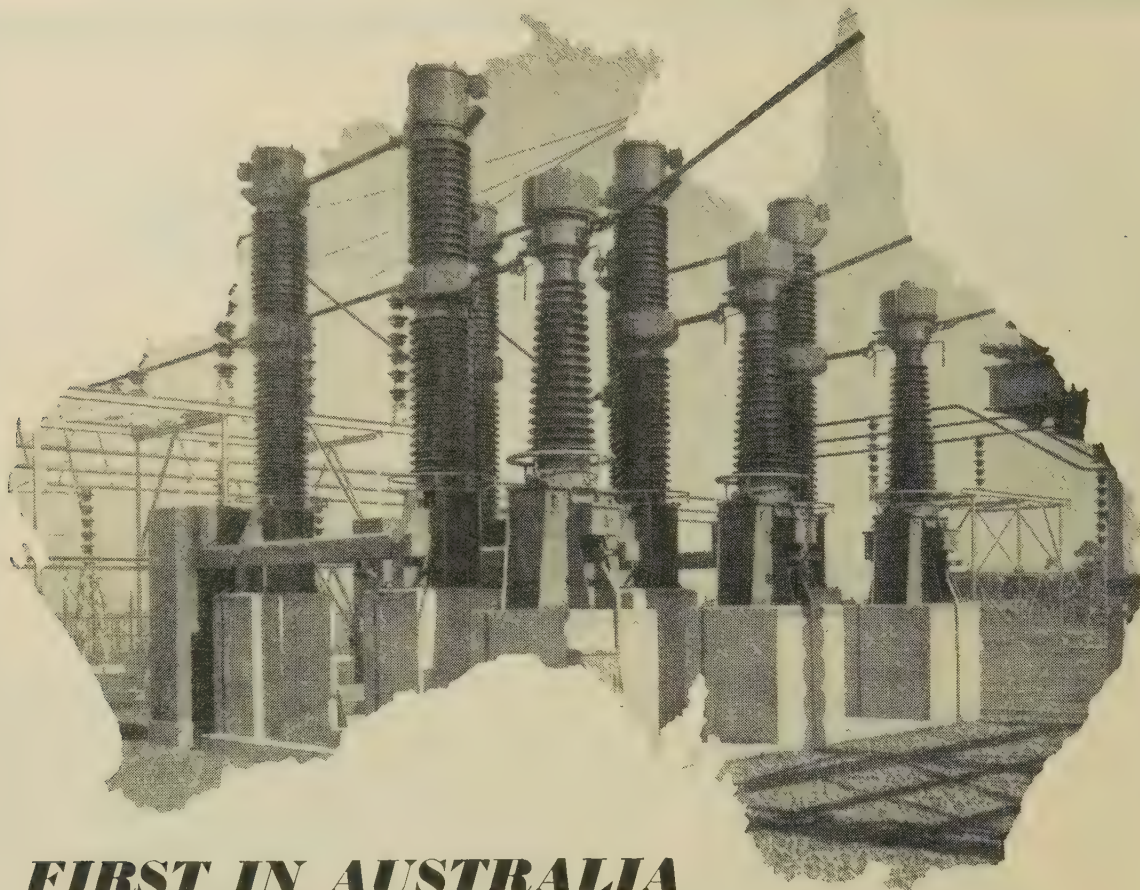
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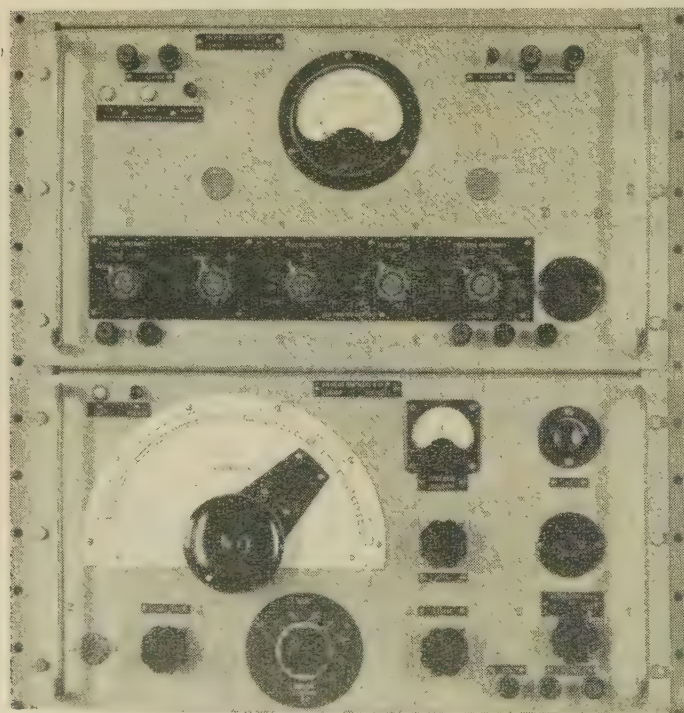
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Frequency range : 300 c/s to 150 kc/s.  
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Accuracy :  $\pm \frac{1}{2}$  db.  
Bridging loss : Not greater than 0.1 db.

#### *Oscillator No. 5*

Frequency range : 300 c/s to 350 kc/s.  
Accuracy : On reaching thermal equilibrium  
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Output impedance : Nominally 600 ohms.  
Output : —46 dbm to +20 dbm.

#### *Transmission Measuring Set No. 4*

Working range : —55 db to +30 db.  
Frequency range : 50 c/s to 50 kc/s.  
Operating impedance : 150, 300, 600 and 1,200 ohms.  
Accuracy :  $\pm \frac{1}{2}$  db.  
Bridging loss : Not greater than 0.1 db.

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Frequency range : 50 c/s to 40 kc/s  
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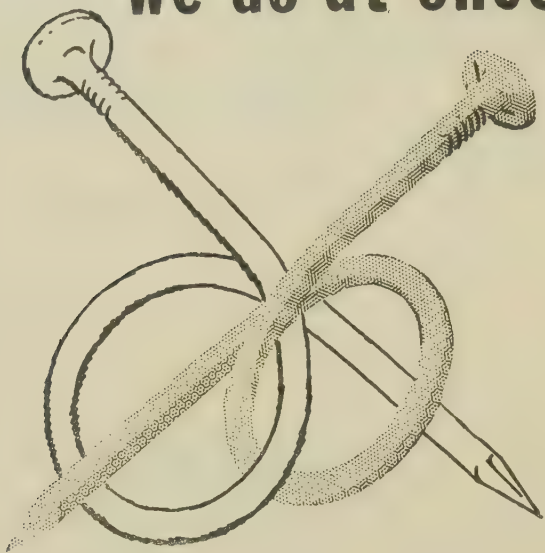
Registered Office: Connaught House, Aldwych, London, W.C.2

RECTIFIER DIVISION: Edinburgh Way, Harlow, Essex

Telephone: Harlow 26811

Telegrams: Sentercel, Harlow

# The difficult we do at once



The Clean Air Bill recognises that the 'impossible'—in this case, the prevention of the emission of dark smoke, grit or harmful gases in certain industrial processes\*—may take a little longer. It recommends that the Alkali Inspectorate should be responsible for ensuring that the best practicable means of controlling pollution from these processes should be used as they become available.

Simon-Carves Ltd have specialised in electro-precipitation and cyclone plant for the prevention of pollution and for the recovery of valuable minerals and other materials for over a quarter of a century. During that time they have pioneered many improvements in design and are today well-equipped to tackle pollution problems in those industrial processes which present special technical difficulties.

*\*Amongst the applications of electro-precipitation one of the most important is the collection of pulverised fuel flue-dust at power stations. Other industrial applications include the recovery of materials from calcining, roasting and smelting processes in metallurgical industries, the recovery of dust from dryers, grinders, crushers and briquetting plants in mining industries, of gypsum, pyrites dust, acid mist, tar fog, phosphate dust, catalysts, etc. in the chemical, petroleum and fertiliser industries, and of process dust from grinders, finishers, lathes, etc. in miscellaneous industries.*

*High efficiency electro-precipitation*

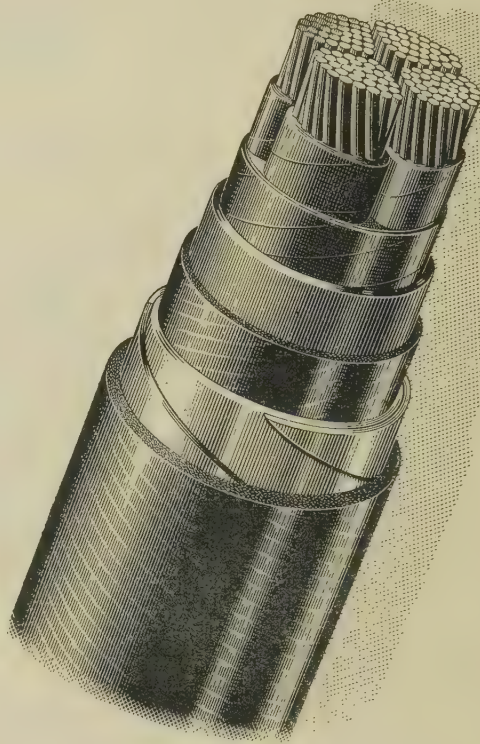
*by Simon-Carves Ltd*



STOCKPORT, ENGLAND

OVERSEAS  
COMPANIES

Simon-Carves (Africa) (Pty) Ltd: Johannesburg  
Simon-Carves (Australia) Pty Ltd: Botany, N.S.W.



*Many cables are made to specification but it is by their attention to the unspecified details that Aberdare's reputation has been built.*

## Aberdare Cables

Paper insulated cables up to 33kV, to BSS or special requirements

ABERDARE CABLES LIMITED

ABERDARE • GLAMORGAN • SOUTH WALES

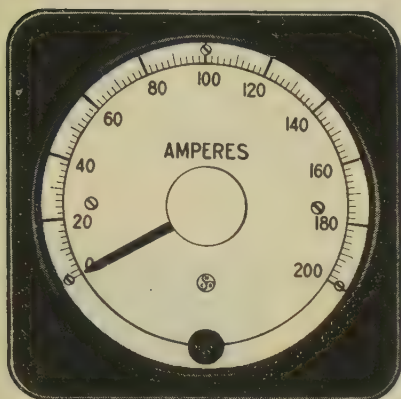
London Office: NINETEEN WOBURN PLACE, W.C.1

*Aberdare Cables are represented in over 40 different territories.  
Names and addresses of agents sent on application.*

# NALDERS



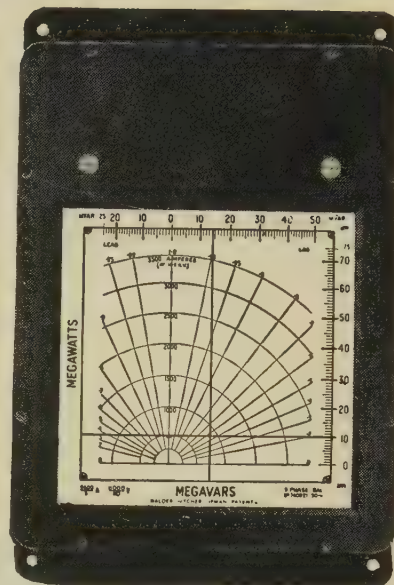
## INSTRUMENTS



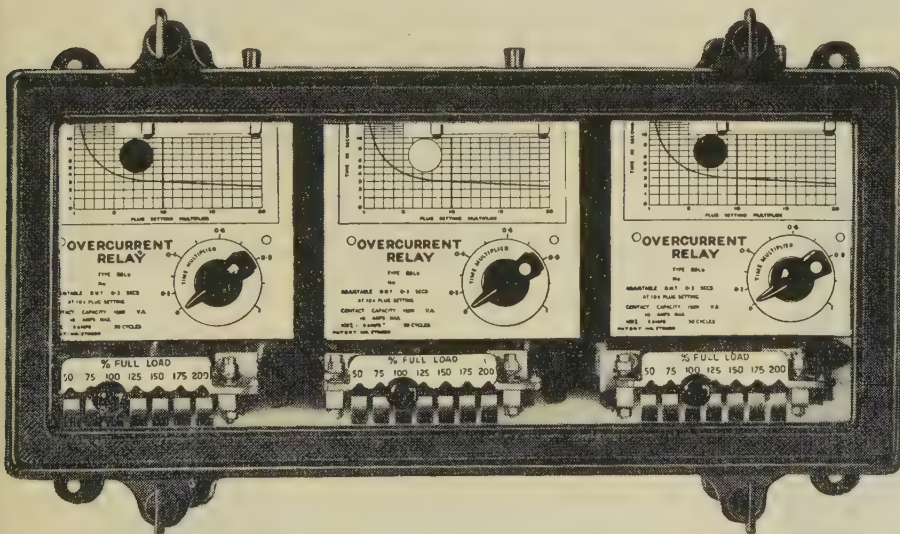
NALDERS N.C.S. Instruments cover a wide range—Indicating, Recording, Switchboard, and Portable. Cases are of die cast aluminium or pressed steel, rectangular, square, or round pattern, finished bright black stove enamel or other colour as specified. We can meet your demands for prompt delivery.

## VECTORMETER

THE N.C.S. VECTORMETER (Nalder-Ivtcher-Lipman Patent) gives simultaneous readings of Megawatts, Megavars, Amperes and Power Factor of the three-phase system in which it is connected. It shows also whether Power is being "Exported" or "Imported." Indication is by two pointers moving at right angles and in straight lines over a common dial. The Megawatt, Megavar and Ampere scales are each 6 ins. long.



## Protective RELAYS



NALDERS PROTECTIVE RELAYS Type B.S.L. are available in a complete range. They are of outstanding quality, having high torque, low energy consumption, and a wide range of adjustable settings. N.C.S. Relays conform in all respects to the requirements of B.S.142.

*We shall welcome your enquiries*

# NALDER BROS & THOMPSON LTD

DALSTON LANE WORKS, LONDON, E.8

Telephone: Clissold 2365 (3 lines)

Telegrams: Occlude, Hack, London

# SPIRAL TUBE

## Coolers

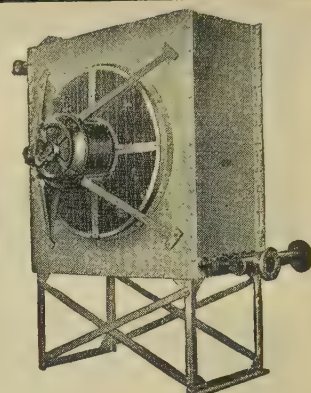
### for the ELECTRICAL ENGINEERING INDUSTRY

The result of 50 years' specialised experience, SPIRAL TUBE Coolers are soundly engineered and robustly constructed for long trouble-free service. Whilst standardisation of design is almost impossible in many in-built units, the Company has, by the extensive use of fabrication, ensured great flexibility of design to meet all requirements, and yet to offer rational designs at low cost. The range extends from cooling coils weighing a few pounds up to multiple section installations weighing several tons.

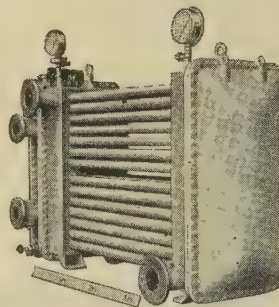
*Write NOW for fully illustrated literature*



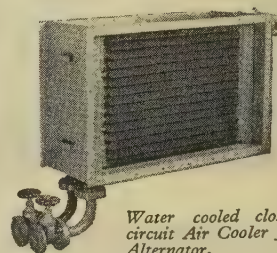
*Water cooled cooling coil for Transformer.*



*Spiral Tube Air Blast Transformer Oil Cooler to dissipate 160 k.w.*



*Water cooled Transformer Oil Cooler for power station to dissipate 200 k.w. and cooling 200 gal. oil min.*

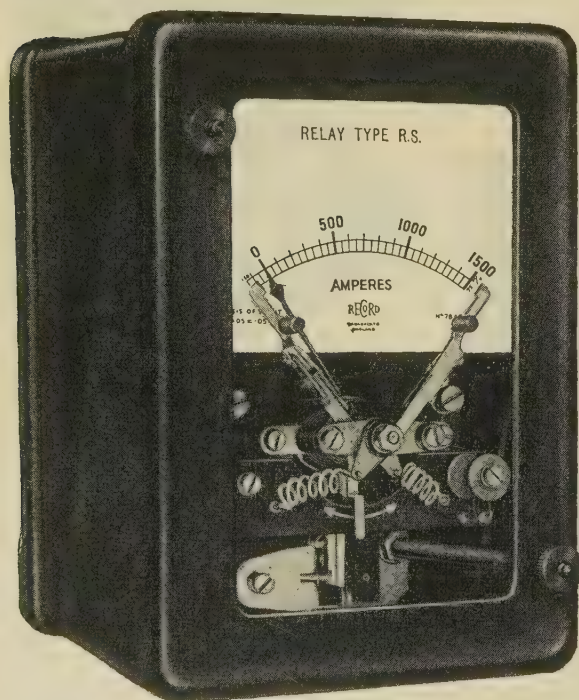


*Water cooled closed circuit Air Cooler for Alternator.*

THE SPIRAL TUBE & COMPONENTS CO. LTD., OSMASTON PARK ROAD, DERBY

Tel.: DERBY 48761 (3 lines)

Head Office: Honeypot Lane, Stanmore, Middlesex. Tel.: Edgware 4658



(SEND FOR LEAFLET K/a)

# Moving Coil Relays

Sensitive • Robust • Accurate • Reliable  
Current • Voltage • Speed • Frequency

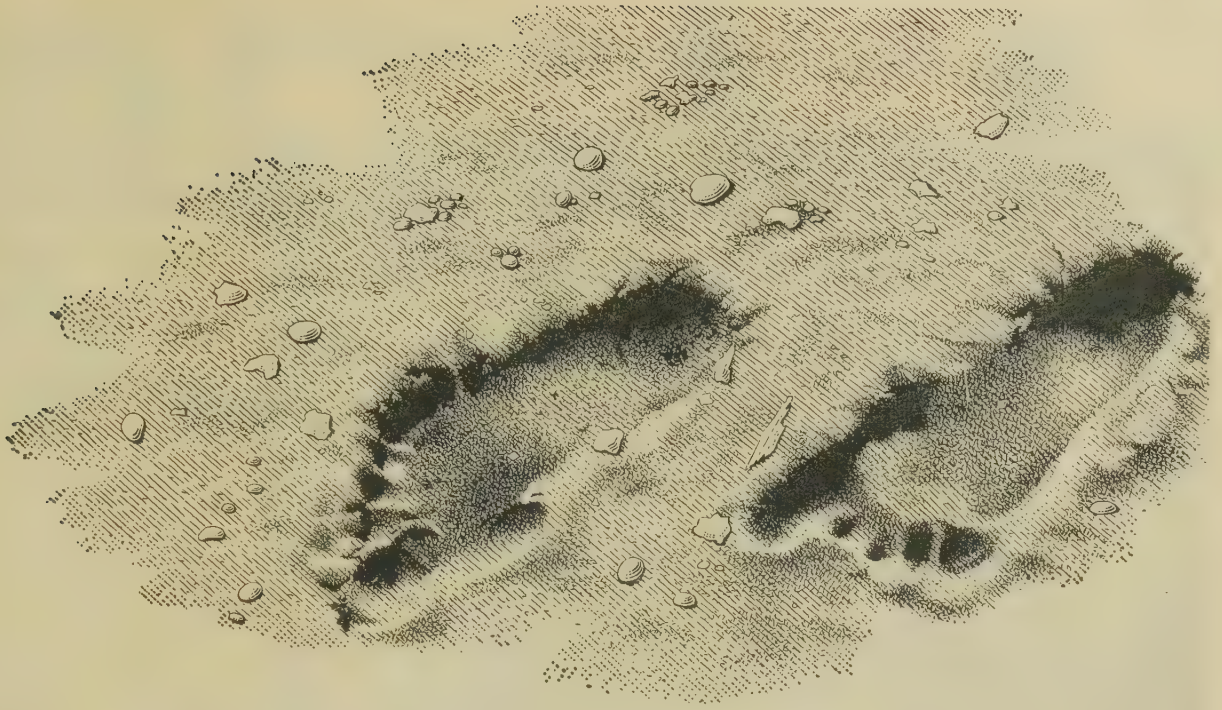


*Switchboard and Portable A.C. and D.C. Indicating and Recording Instruments.*

*"Circscale" Electric Tachometers, Insulation and Resistance Test Sets, Moving Coil Relays, etc.*

# THE RECORD ELECTRICAL COMPANY LIMITED

"CIRSCALE WORKS" • BROADHEATH • ALTRINCHAM • CHESHIRE



# Two feet to an error

A contradiction in term? Depends on the interpretation! The distance between right and wrong can be measured in miles or thousandths of an inch. For instance, the distance from a man's feet to his brain is, on the average, five feet six inches: the merest flexing of a toe is controlled over that distance.

But, like everything human, there comes a time when the signal from the brain is just too late to avoid an error in emergency. A pace less, or a pace more, would have done the trick. Mechanically industry has a similar problem.

The need for positive control of heavy, fast-running machinery, in emergency, is vitally important. Therefore, Dewhurst & Partner have developed

Electro-magnetic brakes which are renowned for their consistent efficiency and provide the answer to certain safe controlled braking.

The practically instantaneous time-response of Dupar Electro-magnetic brakes make them the obvious choice for industrial machinery . . .

and their reputation is universally recognised.

If you have any problem related to the positive control of machinery,

Dewhurst & Partner will be pleased to place at your disposal the wide experience they have gained in many years of service to industry, both at home and overseas.



## DEWHURST & PARTNER LIMITED

INVERNESS WORKS • HOUNSLOW • MIDDLESEX

Telephone: Hounslow 0083 (8 lines)

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DP.3

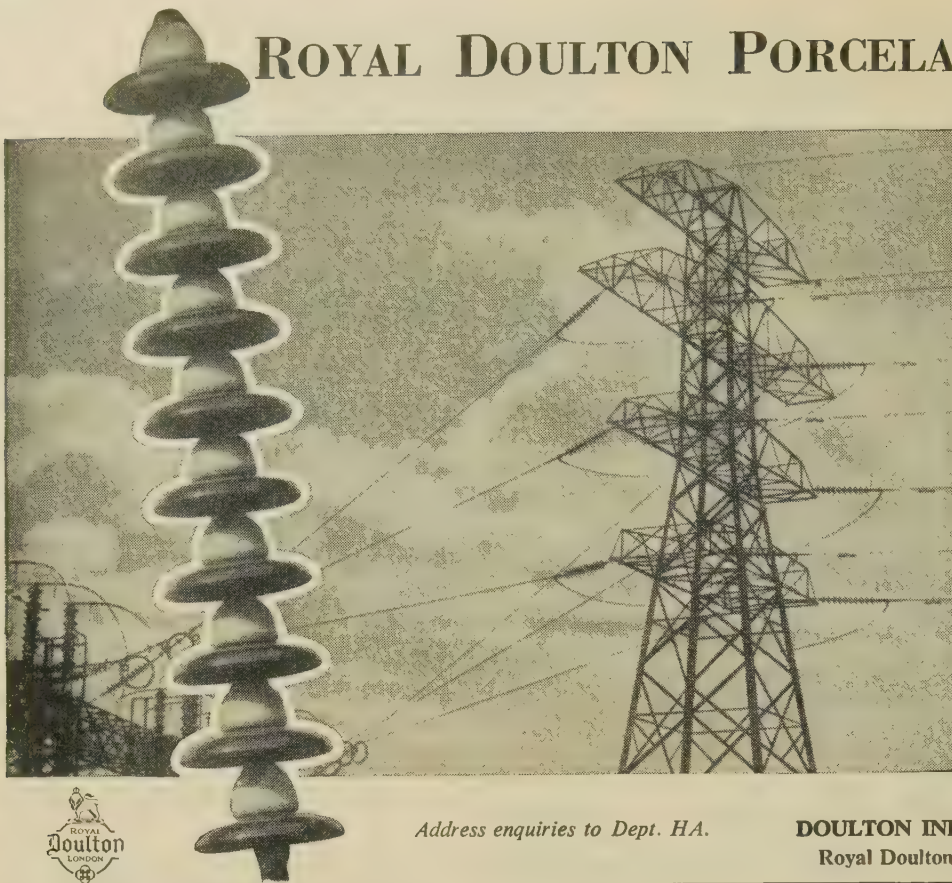
b\*

# ROYAL DOULTON PORCELAIN INSULATORS

... for all  
overhead lines  
and apparatus  
applications

Made by a world-famous British pottery with 141 years' experience in the production of Industrial Ceramics, these insulators embody the same high standards of craftsmanship and fitness for purpose as all other Royal Doulton ceramic products.

*The range includes insulators for*  
Overhead Power Line Transmission;  
Switchgear; Transformers; Sub-  
Stations; Land and Marine Radio;  
Electrified Railways, etc.



Address enquiries to Dept. HA.

**DOULTON INDUSTRIAL PORCELAINS LIMITED**  
Royal Doulton Potteries, Wilnecote, Tamworth, Staffs.

## CARPENTER

## POLARIZED RELAYS

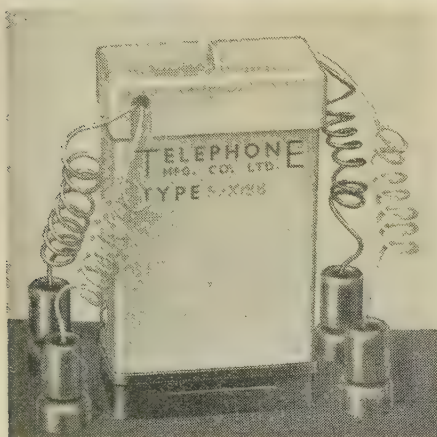
### *Simplify Recording, Control and Test circuits*

The fast operating Carpenter Polarized Relay Type 5PX is being used very satisfactorily in a wide variety of control, recording and test circuits where the available initiating voltage is extremely low and must be greatly amplified before it can be used. The relay enables this to be done efficiently and economically by converting ("chopping") d.c. input signals to a square-wave alternating voltage which may then be amplified simply in an a.c. amplifier.

Alternatively, the small signal voltage can be fed to a straightforward d.c. valve amplifier, while a fraction of the voltage is taken to an a.c. amplifier using an "each-side-stable" Carpenter relay. One side-contact of the relay is used alternately to "earth" and to "free" this input voltage, thereby converting it to square-wave a.c., while the other side-contact demodulates the amplified a.c. output. This output is then fed back to the control grid of the original d.c. amplifier, thereby eliminating any tendency of the output to drift.

The Type 5PX relay has platinum contacts so that contact noise voltages are considerably reduced. Moreover, screening between coil and contact circuits—and flying contact leads—reduce to negligible proportions possible trouble due to "pick-up" from the coil. Where frequencies in excess of 50 c/s are required, specialized versions of the larger Type 3 relay can be used.

These "chopper" relays are successfully incorporated in laboratory test gear, supervisory circuits, temperature recorders, etc., and the Manufacturers will gladly make available to you their experience in this field of electronic equipment.



The Type 5PX Carpenter Polarized Relay is fitted with platinum contacts to reduce thermal noise, and has flying contact leads to reduce "pick-up" in contact circuit due to the coil.

DIMENSIONS :

Height 2.5 in. Width 1.6 in. Depth 0.8 in.  
Approx. weight 4.8 oz.

*Manufactured by the sole licensees*

## TELEPHONE MANUFACTURING CO. LTD

*Contractors to Governments of the British Commonwealth and other Nations.*

HOLLINGSWORTH WORKS . DULWICH . LONDON SE21 Telephone: GIPsy Hill 2211



# C·M·A ranges far and wide !

PRODUCTION of many types and sizes of electric cables is the keynote of the service offered by members of the Cable Makers Association.

It is not by the manufacture of only a few types of cable in popular demand that the C.M.A. members have been able to contribute so handsomely to home and overseas markets. Rather it is by producing a complete range including cables for specialised duties . . . and, most important of all, cables that fulfil the exacting demands of to-day.

Technical advice concerning cables is freely available from members.

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*The Roman Warrior and the Letters "C.M.A." are  
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***Insist on a cable with the C·M·A label***



**CABLE MAKERS ASSOCIATION, 52-54 HIGH HOLBORN, LONDON, W.C.1**

**Telephone: Holborn 7633**

CMA/3

# SIEMENS R.I.N.D. CABLE



## A New Development

The R.I.N.D. Cable is **RADIALLY IMPREGNATED** under strictly controlled conditions thereby presenting a new technique in cable manufacture. The paper insulation is thoroughly dried and impregnated, but contains no superfluous compound. Dimensions in accordance with B.S.480: 1954 specification for normal belted type cables.

R.I.N.D. cable is suitable for general use, but is indispensable for situations where steep gradients and high ambient temperatures are encountered. No special jointing method is necessary, standard joint boxes are used.



### SIEMENS R.I.N.D. CABLE . . .

- \* *Passes Drainage Test in B.S.480/1954*
- \* *Has no critical temperature*
- \* *Gives no overload troubles*
- \* *Causes no difficulty with bending*
- \* *For voltages up to and including 11,000*

SIEMENS BROTHERS & CO. LIMITED,  
WOOLWICH, LONDON, S.E.18.

*Member of the A.E.I. Group of Companies*

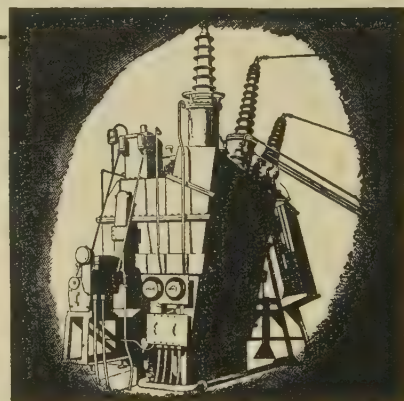
Serving **INDUSTRY**

## DAVENSET

Rectifying equipment  
and  
power transformers

Serving the needs of industry the world over, Davenset rectifiers and transformers are chosen for their reliability under all conditions.

**PARTRIDGE, WILSON & CO. LTD.,**  
Davenset Electrical Works  
**LEICESTER**



### TRANSFORMER AND SWITCH OILS

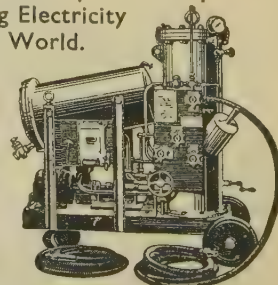
Complete Drying and Purification giving Maximum Breakdown Voltage in one simple operation. No heating. No fire risk. Weatherproof. Fully mobile units. Used by leading Electricity Undertakings throughout the World.

Capacities from 20 gallons  
to 1,000 gallons per hour

## METAFILTRATION

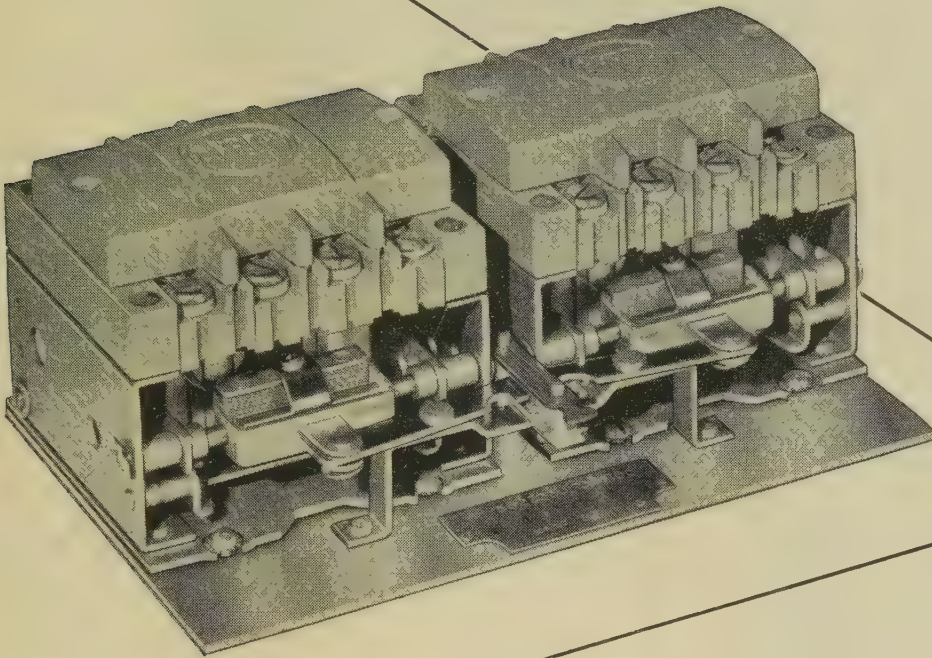
THE METAFILTRATION COMPANY LTD.  
BELGRAVE Rd., HOUNSLOW, MIDDLESEX

★ PHONE: HOUNSLOW 1121/2/3  
GRAMS: METAFILTER, HOUNSLOW



# The smallest

## ● REVERSING CONTACTOR UNIT for RATINGS UP TO 5 H.P.



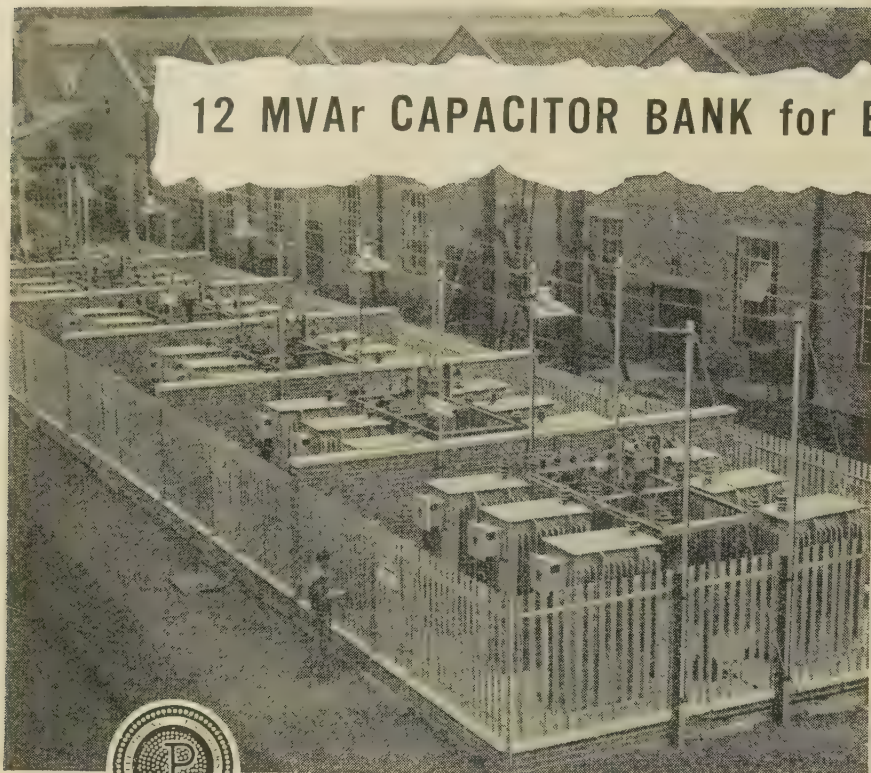
● The unique and advanced design of ARROW contactors makes the mechanically and electrically interlocked unit both the most compact and the lightest to be found anywhere today. When you specify ARROW reversing contactors you can forget your approval worries because the gear complies with British and Canadian standards and also conforms to NEMA and J.I.C. specifications.

Do not fail to obtain a copy of our *new catalogue MS.9*, which gives full details of all ARROW control gear.



ARROW ELECTRIC SWITCHES LTD. hanger lane LONDON W.5

## 12 MVAR CAPACITOR BANK for BRITISH CELANESE LTD.



*J. & P. supply the  
largest single bank in the country.*

With the introduction of a kVA maximum demand tariff for their Spondon Works, British Celanese Ltd. were faced with the problem of improving the overall power factor from 0.8 to 0.97.

For various reasons it was decided that the capacitors should be installed on the high voltage system, and J. & P. were commissioned to supply the required 12 MVAR bank.

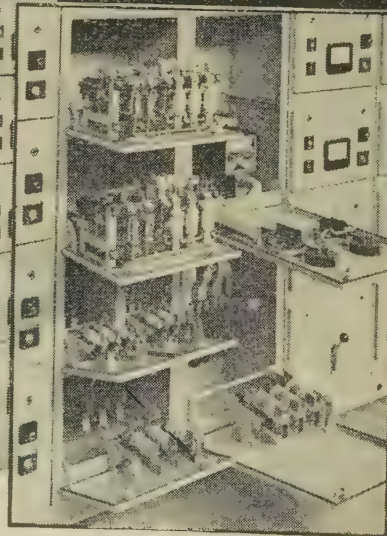
The installation comprises six-2 MVAR sections each containing six-333 kVAR single-phase units star connected for operation on 6.6kV system. The sections are independently switched to provide flexibility. The units were given a special outdoor corrosion resistant finish which included aluminium spraying by the Metallisation process. The urgent nature of the scheme demanded adherence to a quick delivery schedule, which was achieved.

**JOHNSON & PHILLIPS LTD. CHARLTON LONDON S.E.7.**



Patent Pending

## ALL-ISOLATING UNIT TYPE 'Y' CUBICLE SWITCH & CONTROL GEAR



- Complete accessibility to all Starters and Fused Switches.
- Lift off doors for safe and immediate examination.
- Mechanical and Electrical Interlock.
- For Indoor or Outdoor use in any climate.

**ELECTRO MECHANICAL MFG. CO. LTD.**  
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Associated with YORKSHIRE SWITCHGEAR AND ENG. CO. LTD., LEEDS AND LONDON



# BRENTFORD

## Safety

### TRANSFORMERS

*using 'Class h' insulation*

**for greater safety  
and reliability . . .**

**BRENTFORD TRANSFORMERS LTD.,  
announce a new range of dry (oil-less)  
transformers using glass, ceramics  
and other similar inorganic materials  
impregnated with silicone resins.**

*This latest development in transformers  
offers greatest ever safety for:—*

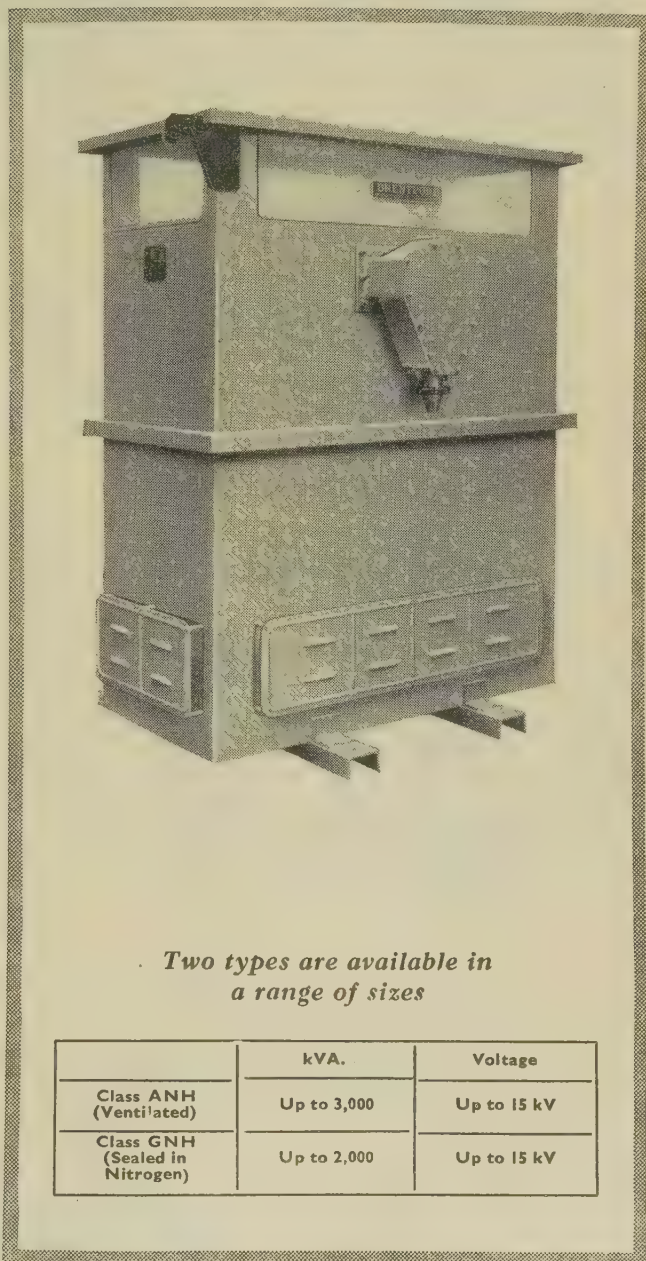
Atomic Energy Establishments — Chemical Works —  
Oil Refineries — Explosives Factories — Blocks of Flats  
— Schools — Passenger Vessels — Tankers — Coal  
Mines — Radio, Radar and Television Stations —  
Underground Railways.

### **BRENTFORD GREEN SEAL SAFETY TRANSFORMERS**

*offer all these advantages:—*

- Safest transformers ever developed
- No fire or explosion hazard
- Carry the lowest fire insurance rates
- Least affected by water
- Exceptionally high overload capacities
- Minimum maintenance — even in highly contaminated areas

**SEND FOR FULLY DESCRIPTIVE  
BROCHURE**



*Two types are available in  
a range of sizes*

	kVA.	Voltage
Class ANH (Ventilated)	Up to 3,000	Up to 15 kV
Class GNH (Sealed in Nitrogen)	Up to 2,000	Up to 15 kV

# **BRENTFORD TRANSFORMERS LTD.**

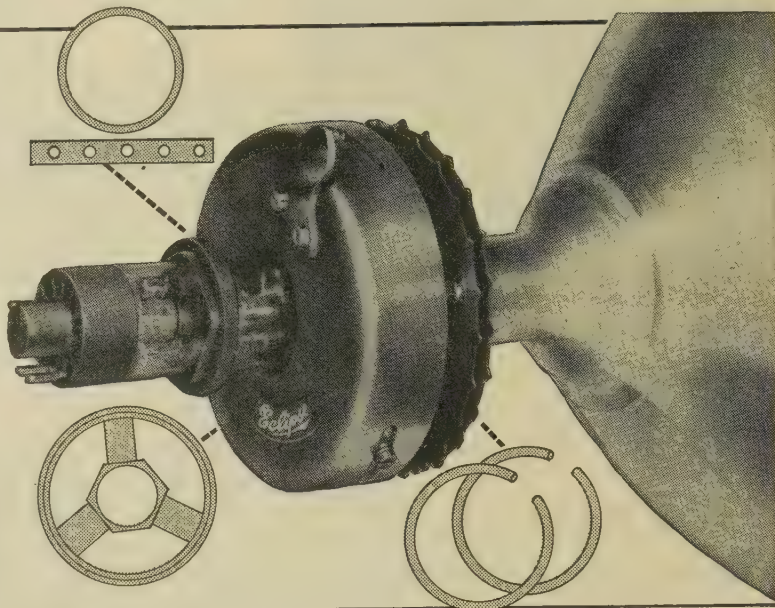
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Telephone: Crawley 25121



## PERMANENT MAGNETS for TELEVISION RECEIVERS

Make sure you have a copy of the latest 1955 edition of publication P.M. 112 "Permanent Magnets for Television Receivers." For loud speaker magnets see publication P.M. 108.

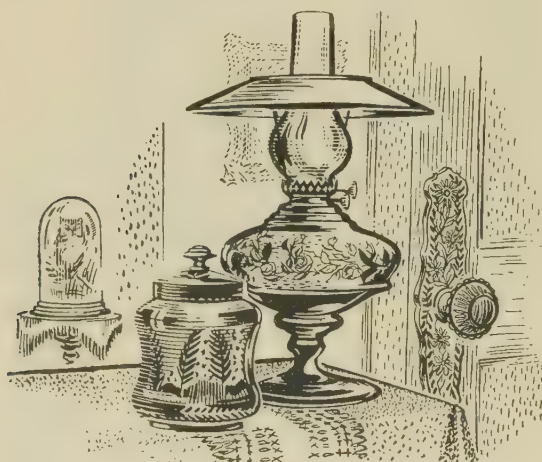


MADE BY THE DESIGNERS & MANUFACTURERS OF ECLIPSE PERMANENT MAGNET CHUCKS  
**JAMES NEILL & CO. (SHEFFIELD) LTD.**

SHEFFIELD 11

ENGLAND

M 4

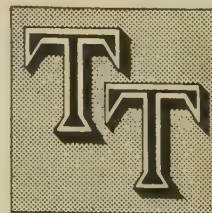


## TOBACCO JARS AND DOOR KNOBS

If ceramic door furniture, hermetically sealed tobacco jars, and lamp containers for silversmiths had not been the fashion in the 'sixties', Taylor, Tunnickliff would not be making the world's finest porcelain insulators today, for it was this demand for precision pottery that brought together Thomas Taylor, an engineer, and William Tunnickliff, a potter. They combined their separate skills to manufacture the ceramic parts with greater precision than was hitherto possible.

From the beginning they were successful, and owing to the foresight and determination of Mr. Taylor, who in the late nineteenth century saw the future of this new electricity, laid the foundations of the industry that today have made Taylor, Tunnickliff masters of porcelain insulation.

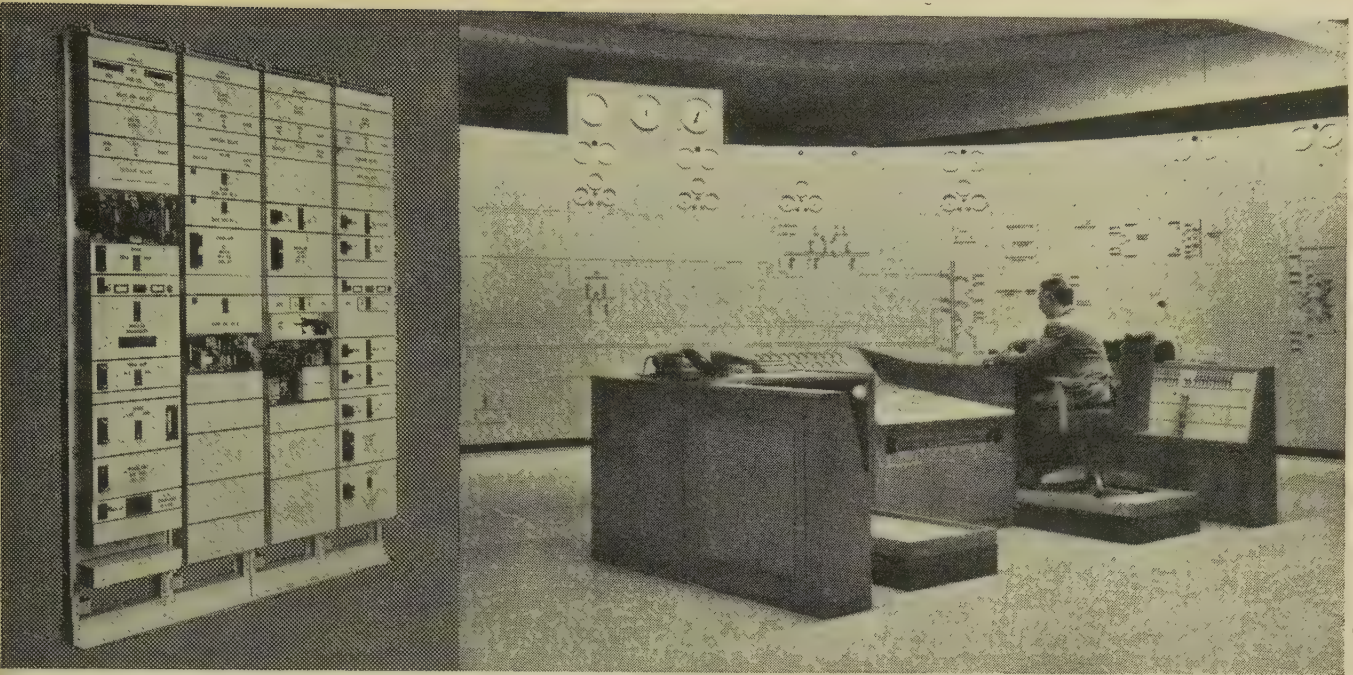
OVERHEAD LINE INSULATORS  
SWITCHGEAR INSULATORS  
BUSHES AND BUSHINGS  
DIE MADE ARTICLES FOR LOW VOLTAGE APPLICATIONS  
CERAMICS FOR RADIO FREQUENCIES  
REFRACTORIES FOR HEATING APPARATUS  
FISH SPINE BEADS, ETC.



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Stoke-on-Trent 25272-5

London Office : 125 HIGH HOLBORN, W.C.1  
Holborn 1951-2



# INFORMATION

## by the quickest possible means

To the power engineer, transmission of information by the quickest possible means is all-important. No one man can be in a number of places at once, but the same effect is achieved with a reliable communication system. G.E.C. can provide such a system whether the need is for speech facilities, long-range meter readings, state-of-switchgear indications, etc. The method varies according to circumstances—but the result is always the same: greater certainty, increased efficiency, and easier, smoother working.

Use the experience of G.E.C. to surmount your difficulties.

### POWER-LINE CARRIER SYSTEMS

These provide up to eight communication circuits over the power lines themselves. Each composite circuit accommodates a telephone circuit, a telephone-signalling channel, independent channels for teleprinter working, and remote switchgear control and metering. The carrier signals are injected into the high-tension line via broad-band coupling equipment.

### REMOTE SUPERVISORY CONTROL

A system for controlling power distribution using equipment and techniques developed from the selection and signalling devices of automatic telephony. Meter readings, switchgear indicators and control signals are returned over the same channels.

### PRIVATE AUTOMATIC EXCHANGE

P.A.X. equipment provides a reliable and flexible telephone system. Multi-line conferences and priority for emergency calls are two of the many facilities that can be incorporated in this equipment.

### RADIO

VHF multi-circuit radio links are recommended for use over rough country where line or cable systems are difficult and uneconomical.



**EVERYTHING FOR TELECOMMUNICATIONS BY OPEN-WIRE LINE,  
CABLE AND RADIO, SINGLE OR MULTI-CIRCUIT, OR TV LINK.  
SHORT, MEDIUM OR LONG HAUL**

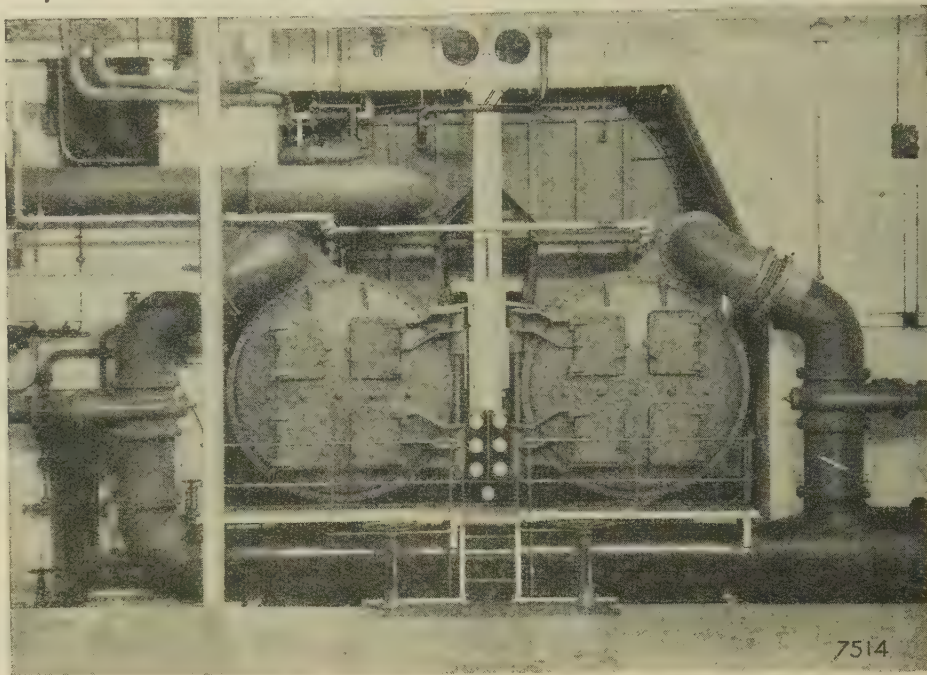
*Mirrlees'*

## Specialised Equipment for **POWER STATIONS**

Condensing Plant  
Evaporating Plant  
Feed Heating Plant  
De-aerating Plant  
Pumping Plant  
Turbines for auxiliary drives

Illustrated is a MIRRLEES Surface Condensing Plant operating in conjunction with a 25,000 kW Alternator in a South African Power Station.

MIRRLEES Equipment has been supplied for the Power Plants of many Electricity Authorities and Industrial Undertakings in Britain and Overseas.



Photograph by courtesy of B.T.H. Co. Ltd.

**THE MIRRLEES WATSON COMPANY LIMITED**  
SCOTLAND STREET GLASGOW C.5      38 GROSVENOR GARDENS LONDON S.W.1

WRITE FOR BOOKLET ON THIS SUBJECT

**ELIMINATE  
COMPOUND  
DRAINAGE...**

... by using **GLOVERS  
STANDARD PAPER INSULATED  
CABLES** which are  
**NON-DRAINING** in any situation.

**GLOVERS STANDARD CABLES**  
"for normal distribution work can  
be used for vertical installation  
without any special precautions  
being necessary..."

**W.T. GLOVER & CO. LTD.**

TRAFFORD PARK MANCHESTER 17  
TRAFFORD PARK 2141

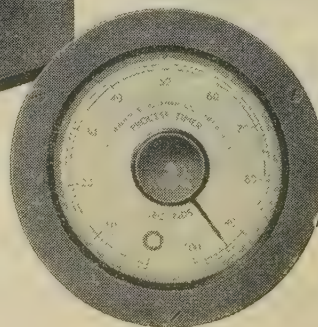
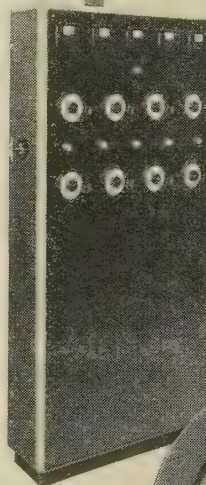
## CHAMBERLAIN & HOOKHAM TYPE P PROCESS TIMERS

FOR ACCURATE AND  
AUTOMATIC PROCESS CONTROL

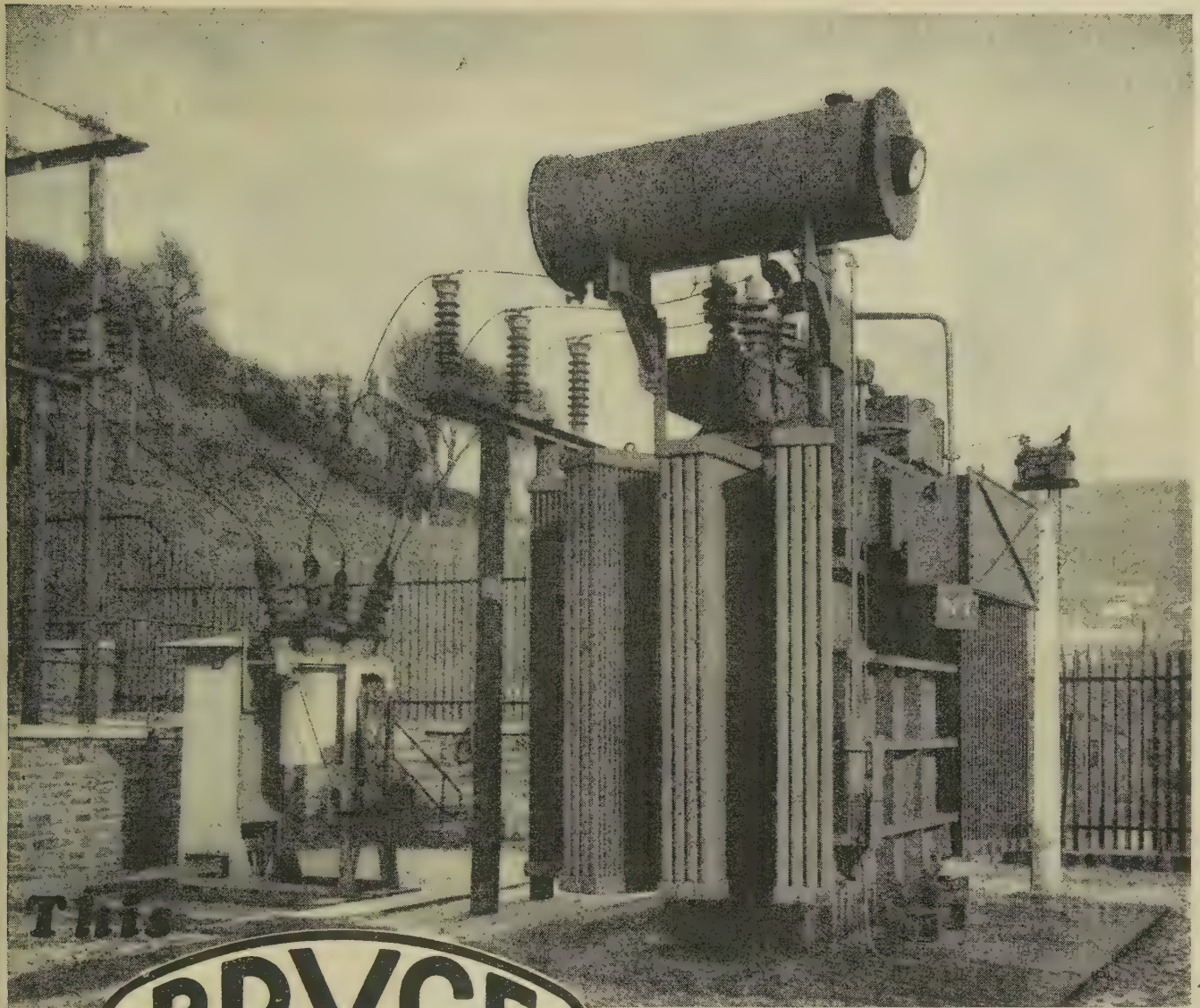
- ★ Scale ranges from 0-10 secs. up to 24 hours.
- ★ Settings down to 1/10 sec.
- ★ Accuracy within 0.25% of full scale range.
- ★ Available as single units for self-mounting or as complete control panels.
- ★ Any operation requiring time control by electrical means can be regulated by this instrument.

*Chamberlain  
&  
Hookham*

**CHAMBERLAIN & HOOKHAM LTD.**  
BIRMINGHAM



TYPE P PROCESS TIMER  
CAT. SECTION 11300

**BRYCE****TRANSFORMER**

is one of two 10,000 kVA. 33,000/11,000 volts, 3 phase, 50 cycles, transformers installed for the National Coal Board in South Wales.

As in every Bryce installation, its dependability in service results from meticulous attention to detail at every stage of design and construction.

*We build Power Transformers of all types up to 20,000 kVA. and are also one of the principal British manufacturers of Power Capacitors.*

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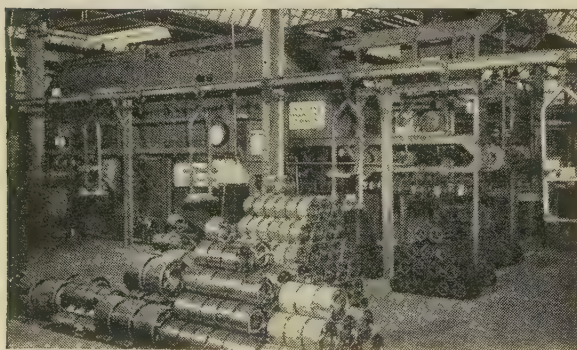
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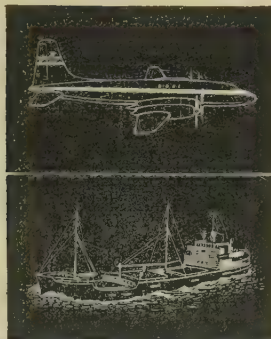
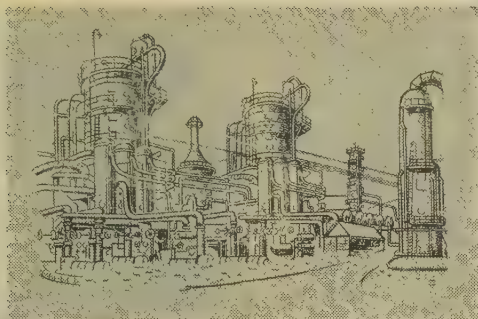
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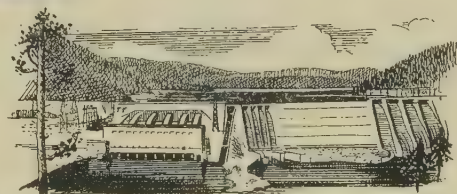
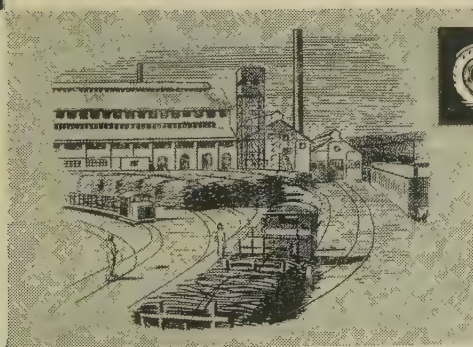
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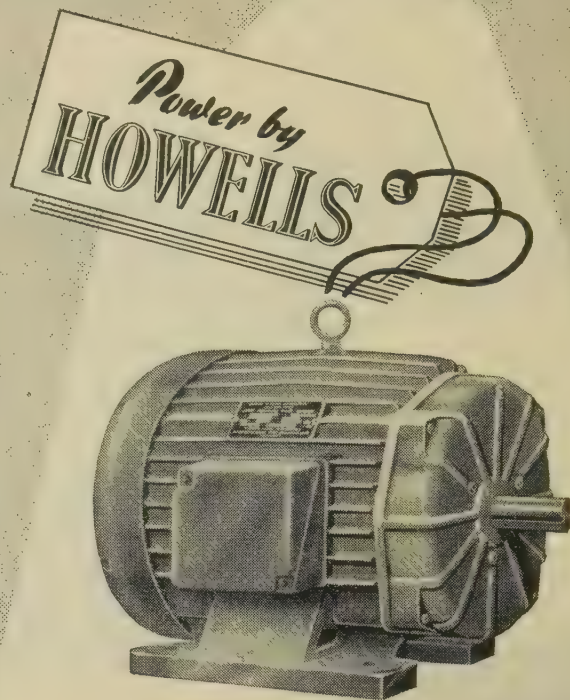
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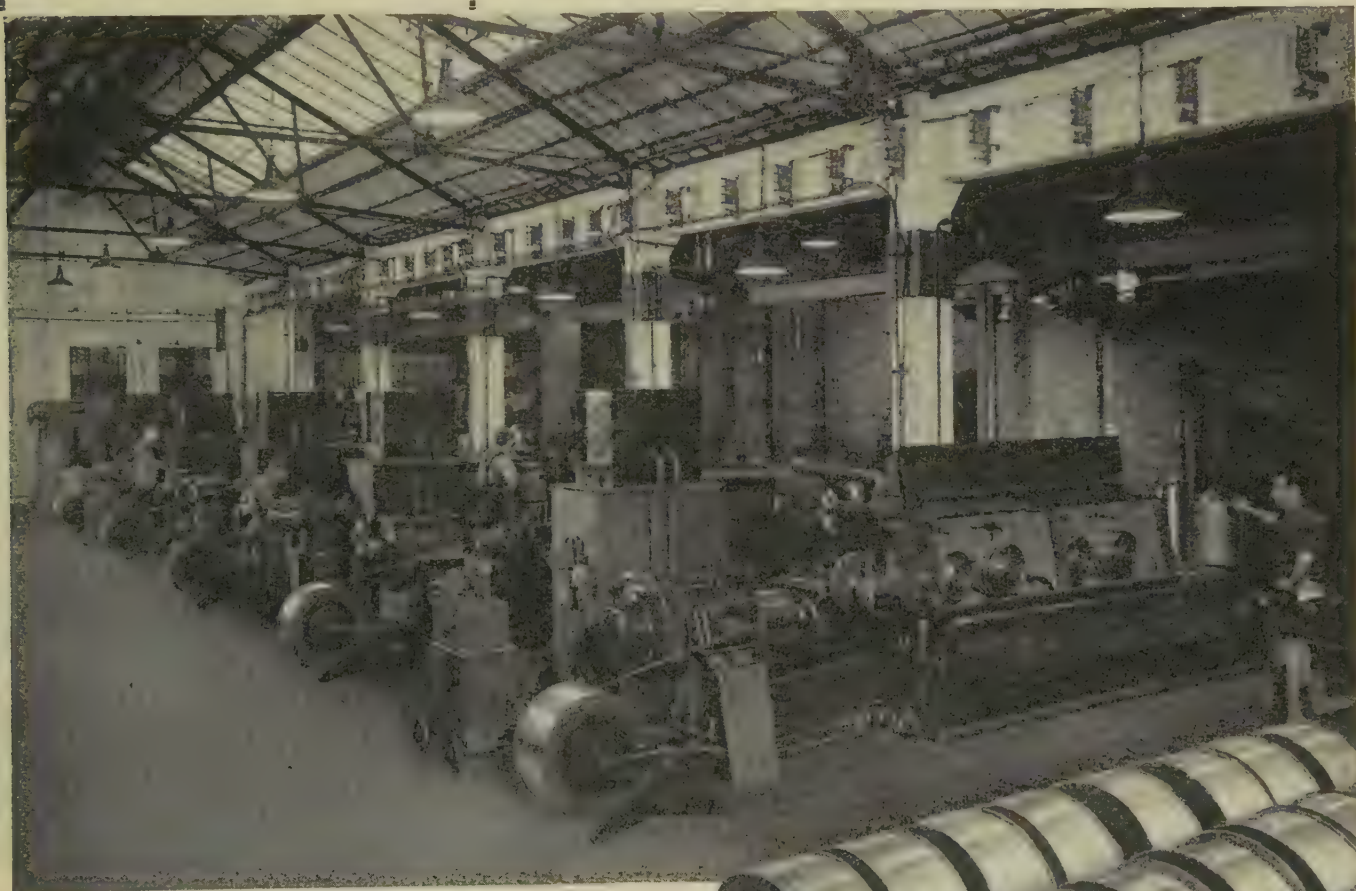
(a) 19 Tubular  
Copper stranded  
with 18 Solid  
Copper.  
1" overall diameter.



(b) 37 Tubular  
stranded.  
1" overall diameter.



(c) 19 Tubular  
stranded.  
0.72" overall diameter



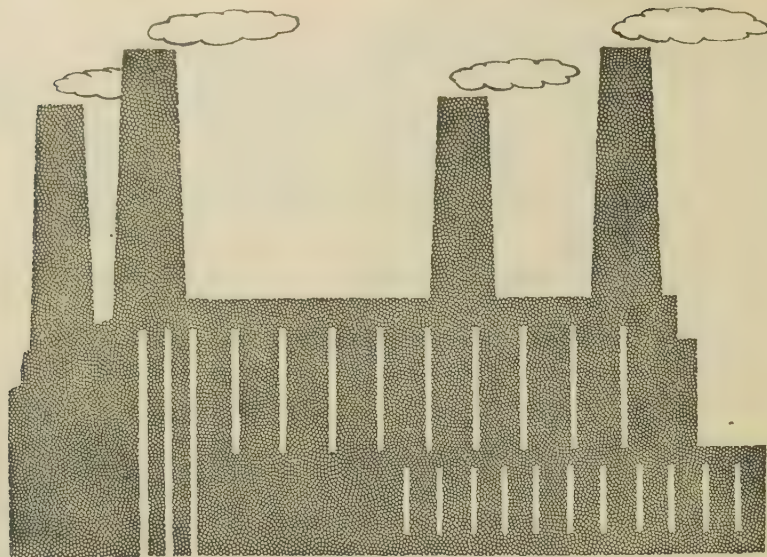
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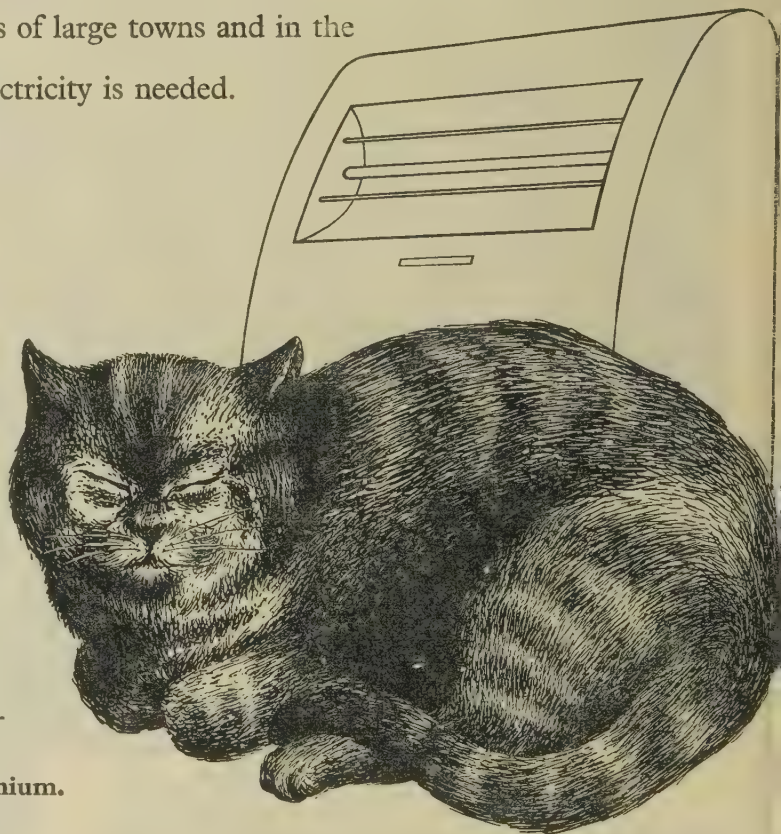
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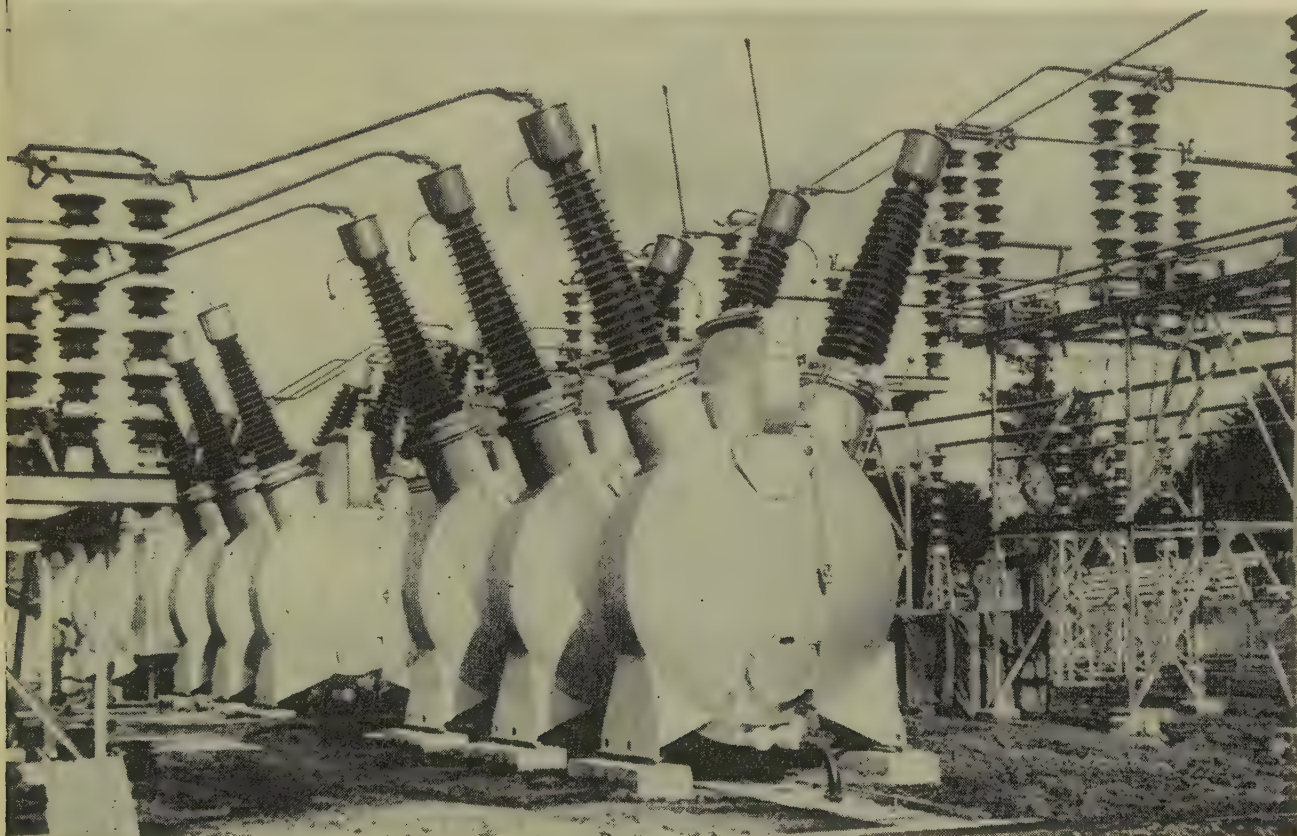
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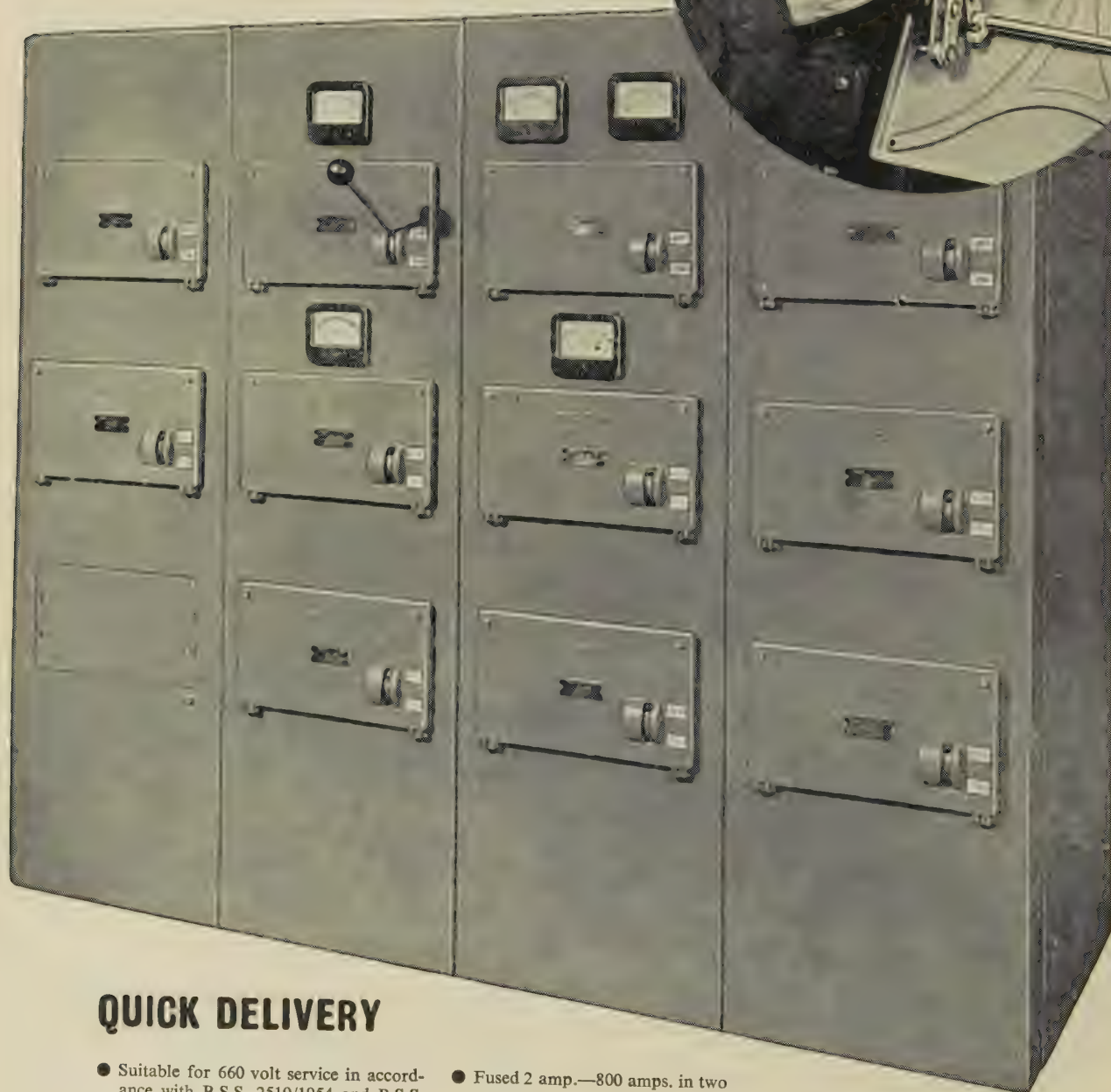
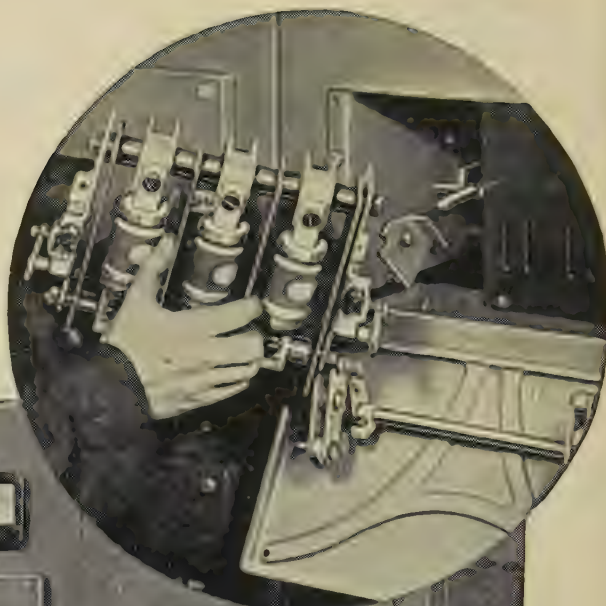
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## GERMANIUM AND SILICON POWER RECTIFIERS

By T. H. KINMAN, M.B.E., Member, G. A. CARRICK, M.A., R. G. HIBBERD, B.Sc., Associate Member, and A. J. BLUNDELL, Associate Member.

(The paper was first received 22nd June, and in revised form 11th August, 1955. It was published in October, 1955, and was read before THE INSTITUTION 10th November, 1955, the EAST ANGLIAN SUB-CENTRE 31st January, the RUGBY SUB-CENTRE 1st February, the NORTH-WESTERN CENTRE 7th February, the SOUTH-WEST SCOTLAND SUB-CENTRE 29th February, the NORTH-EASTERN CENTRE 12th March, and the SOUTHERN CENTRE 14th March, 1956.)

### SUMMARY

The special properties of the semi-conductors germanium and silicon have recently been used for power conversion; equipments have been made with germanium rectifiers rated at 300 and 1000 kW, with conversion efficiencies exceeding 97%, and having other advantages over older types of convertor.

The history of this development is first briefly given, followed by a short explanation of rectification phenomena in a single crystal having a  $p$ - $n$  rectifying junction formed by the disposition of positive and negative current carriers in the body of the crystal. A distinction is made between two types of  $p$ - $n$  junctions produced: one in growing the crystal and the other by a subsequent fusion process in the wafer of a single crystal. The latter is shown to be the preferred type for power rectifiers.

Steps in the production of the rectifier, the various types made by one organization, the methods of rating and the electrical tests applied to low-, medium- and high-power units are then described.

The latter half of the paper is devoted to the special features of the high-power unit, rated up to 2 kW. Tests are specified for series and parallel operation, methods of cooling and relative efficiencies are discussed and comparisons are made with other convertors. Typical installations are cited, one of which has been successfully operated for two years. An 18 MW installation in the course of construction is also mentioned.

The paper is optimistic about the future of  $p$ - $n$  junction devices. Data are given showing the unique properties of silicon, with its ability to operate at much higher temperatures than germanium. Thus, when more economically producible, silicon devices will be extensively used.

A comprehensive list of references to earlier work is given, supplemented by Appendices. These include a fuller explanation and mathematical treatment of current flow in semi-conductors. The phenomena of inverse voltage breakdown and hole storage are also explained in greater detail, since these effects have special significance in operating the high-power fused-junction rectifier.

### LIST OF SYMBOLS

$D_e$  = Diffusion coefficient for electrons.  
 $D_h$  = Diffusion coefficient for holes.

$d$  = Thickness of wafer of fused rectifier.

$\mathcal{D}$  = Electric displacement.

$E$  = Electric field ( $d\phi/dx$ ).

$e$  = Electronic charge.

$G$  = Rate of generation of carriers.

$I$  = Current.

$I_s$  = Inverse saturation current.

$I_f$  = Peak forward current.

$I_r$  = Peak inverse current.

$J$  = Current density.

$k$  = Boltzmann's constant.

$n$  = Particle concentration.

$n_d$  = Total concentration of donors.

$n_e$  = Concentration of electrons.

$n_h$  = Concentration of holes.

$n_{ta}$  = Total concentration of acceptor traps.

$n_{td}$  = Total concentration of donor traps.

$\bar{n}_{d+}$  = Equilibrium concentration of charged donors.

$\bar{n}_e$  = Equilibrium concentration of electrons.

$\bar{n}_h$  = Equilibrium concentration of holes.

$\bar{n}_i$  = Equilibrium concentration of electrons in pure germanium (intrinsic concentration of electrons).

$\bar{n}_{ta-}$  = Equilibrium concentration of charged acceptor traps.

$\bar{n}_{td+}$  = Equilibrium concentration of charged donor traps.

$P$  = Total internal power loss.

$R$  = Rate of recombination of carriers.

$S$  = Thermal resistance.

$T$  = Absolute temperature.

$t$  = Time.

$V_a$  = Applied voltage.

$V_r$  = Inverse voltage.

$V_f$  = Maximum peak forward voltage.

$V_r$  = Maximum peak inverse voltage.

$X$  = Chemical field ( $d\xi/dx$ ).

$\Gamma$  = Flow of particles.

$\epsilon_r$  = Relative permittivity.

$\epsilon_0$  = Absolute permittivity.

$\theta_a$  = Ambient temperature ( $^{\circ}\text{C}$ ).

$\theta_j$  = Junction temperature ( $^{\circ}\text{C}$ ).

$\mu$  = Mobility.

This is an "integrating" paper. Members are invited to submit papers in this category, giving the full perspective of the developments leading to the present practice in a particular part of one of the branches of electrical science.

The authors are with the British Thomson-Houston Co., Ltd.

$\xi$  = Chemical potential.  
 $\sigma$  = Conductivity.  
 $\tau$  = Lifetime of carriers.  
 $\psi = \int E dx$ .

### (1) INTRODUCTION

The phenomenon of rectification was first observed in solids over a century ago, but little practical use was made of it until the crystal detector appeared early in the present century, followed by the metal rectifier many years later.<sup>1</sup> A common feature of both devices is the use made of two dissimilar metallic elements in contact, with a rectifying barrier disposed at their interface. They differ fundamentally, however, in the manner of their construction and power handling capacity.

A new type of rectifier has recently been developed in which the rectifying barrier is integral with the body of a semi-conductor, such as germanium or silicon, thus utilizing the special properties of these elements and achieving extraordinarily high rectifying efficiencies and power handling capabilities. It is known as the *p-n* junction rectifier. This important development was the direct result of a renewed interest in semi-conductors following the successful use of the crystal valve in radar, where greater efficiency and other advantages led to its use in place of the thermionic valve as a frequency converter for the reception of microwaves.<sup>2</sup>

Silicon was found to be the most suitable material for this purpose, but many other semi-conductors were examined, notably germanium. Consequently, a much better understanding was obtained of the physical and electrical properties of these materials.

A further advance was made in 1948<sup>3</sup> when germanium and silicon devices with two point contacts instead of one were made to simulate the functions of the 3-electrode valve in amplifiers or oscillators. However, the paper describes, almost exclusively, types of *p-n* junction rectifier and the techniques of making them, as developed by one particular organization, based initially on earlier work carried out in America.

### (2) THE *p-n* JUNCTION RECTIFIER

Shockley<sup>4</sup> has already described the bulk properties of semi-conductors and the theory of rectification; it is therefore necessary to explain only briefly the physical composition of the device and its behaviour under operating conditions.

Fig. 1(a) shows the section of a single crystal in which two regions are marked *p* and *n* to denote the presence of different impurity atoms, having one less and one more valence electron than the semi-conductor atoms respectively. The *p*-region contains a number of acceptor atoms, called so because the missing electron constitutes a vacancy or positive hole in the crystal lattice, whereas in the *n*-region the impurity atoms donate electrons and thus increase the number in the lattice. A junction is thus formed between the two regions, as indicated by the broken line.

If now a circuit is completed through the rectifier, as shown in Fig. 1(b), current will flow in the forward direction as follows: free electrons from the *n*-region will pass across the junction and combine with the positive holes, to be replaced by further holes flowing into the *p*-region under the influence of the applied electric field. Simultaneously, holes from the *p*-region pass across the junction and are mostly filled by electrons before reaching the base contact, since the *n*-region will now be filled with electrons flowing in from the external circuit.\*

When the polarity of the applied field is reversed, electrons

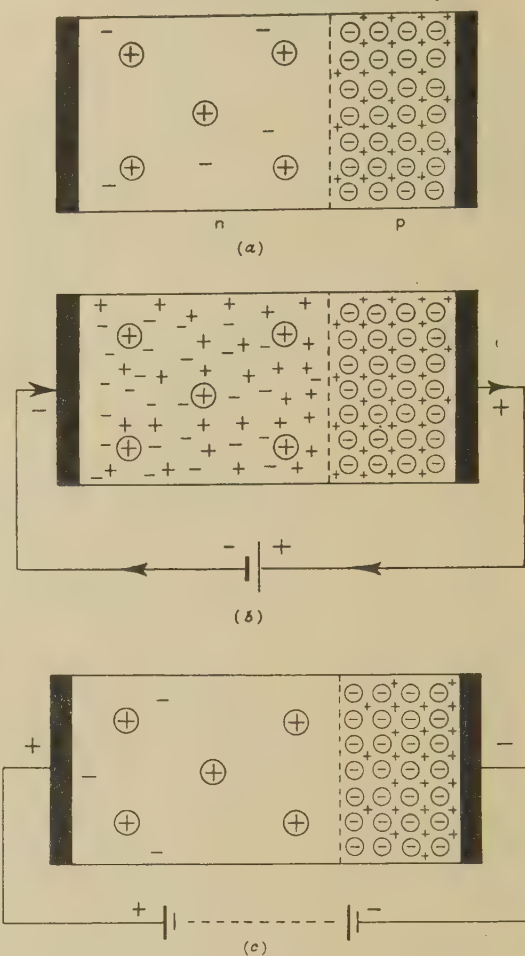


Fig. 1.—*p-n* junction rectifier.

⊕ Donor atom.  
 ⊙ Acceptor atom.  
 + Hole.  
 - Electron.

(a) Static state.  
 (b) Forward current connection.  
 (c) Reverse current connection.

in the *n*-region and positive holes in the *p*-region will move away from the junction, leaving the positive donor atoms and the negative acceptor atoms no longer surrounded by oppositely charged electrons and holes. The two regions on each side of the junction are therefore left with net positive and negative charges respectively, as illustrated in Fig. 1(c), much resembling the condition of a charged capacitor with a potential difference between its terminals. As the reverse potential is increased the electrons and holes will move further away from the junction, until the potential so produced is equal to the applied voltage, when the current almost ceases.

The reverse current, however, is not quite zero, because of a phenomenon peculiar to all semi-conductors. The atoms in a crystal are in a state of thermal agitation which increases with temperature. This causes a simultaneous generation of electrons and holes, and in the absence of an electric field these diffuse through the material until an electron encounters a hole and recombines. In relatively pure germanium the life of a generated hole is of the order of 2 millisecc. Consequently, the generation of these electron-hole pairs throughout the material and in the region of the junction, where a high voltage-gradient exists, will give rise to a small and constant reverse current, theoretically independent of the applied voltage but increasing rapidly with

\* Another explanation of the forward current in a fused-junction rectifier is given in Section 14.1.6.

temperature; very approximately it doubles itself for each  $10^{\circ}\text{C}$  rise.

Breakdown in the reverse direction may be caused either by the increase of this saturation current with temperature, progressively stepping up the generation of current carriers, or by the voltage gradient becoming sufficiently great to tear electrons out of their orbits in the crystal structure. The voltage at which this occurs is known as Zener breakdown voltage, after the physicist who first observed the effect.

There are two types of  $p$ - $n$  junction: one produced while growing a single crystal, by heat treatment of the grown crystal, by nuclear bombardment or by a diffusion process;<sup>5</sup> and the other, developed later by Hall and Dunlap,<sup>6</sup> called the fused junction, and formed by fusing one impurity element into one surface and another impurity element into the opposite surface of a thin wafer of single-crystal germanium or silicon and so producing a  $p$ - and an  $n$ -region separated by the junction.

The essential structural difference between these two types of rectifier is illustrated in Fig. 2, where Fig. 2(a) shows the grown

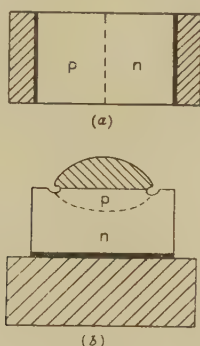


Fig. 2.—Types of  $p$ - $n$  junction.

(a) Grown.  
(b) Fused.

type, and Fig. 2(b) the fused type. Their electrical characteristics differ significantly, and although both are highly efficient as rectifiers, the fused-junction type permits of much higher current densities and is easier to make. Thus, the fused junction is at present the preferred type for power devices, but other methods of producing the junction may ultimately be used when further developed.

### (3) PREPARATION OF SEMI-CONDUCTOR MATERIALS

Since the characteristics of the rectifier depend so much on the initial preparation of the material, some account of the production processes will first be given briefly. The steps taken in producing single-crystal germanium may conveniently be classified under the following headings: extraction of germanium from natural sources and chemical purification; reduction of the dioxide to metal; refining the metal to an intrinsic state of purity at room temperature; adding impurity elements to the germanium; pulling of a single crystal; and methods of control.

#### (3.1) Germanium Dioxide

There is now an abundant supply in Britain of germanium residues recoverable from flue dust. Economic recovery processes have been described by Powell and others<sup>7</sup> following the earlier work of Morgan and Davies,<sup>8</sup> but the state of purification achieved by the most modern chemical processes falls far short of that required by the manufacturer of the rectifying devices.

Analyses of germanium recovered from flue dusts show it to contain many injurious elements that must be removed before it is acceptable for semi-conductor devices.

Arsenic was identified as a harmful impurity present in the dioxide, but improved methods of chemical purification and analysis, stimulated by a D.S.I.R. committee,<sup>9</sup> reduced the arsenic content to less than 0.25 part per million; much less, in fact, than is permitted by statutory regulations to occur in food. To make good high-voltage rectifiers, however, the impurity content must be of the order of one part in a thousand million, and great care is necessary in protecting the material from exposure to contamination after purification and in each subsequent production process.

#### (3.2) Reduction to Metal

In the reduction of germanium dioxide to metal the heating cycle in a hydrogen atmosphere is arranged by temperature control to minimize the loss of germanium in the form of volatile products.

Reduction of  $\text{GeO}_2 + 2\text{H}_2$  to  $\text{Ge} + 2\text{H}_2\text{O}$ , for example, occurs at  $700^{\circ}\text{C}$ . After reduction, the temperature is raised well above  $936^{\circ}\text{C}$ , the melting point of the metal, and then, following fusion, the metal is allowed to solidify and cool over a period of several hours.

#### (3.3) Zone Refining<sup>10</sup>

Zone refining is a comparatively recent development, an advance on an earlier recrystallization method commonly used for metal refining. The germanium is placed in a graphite boat surrounded with pure inert gas, and is pulled through a number of high-frequency heating coils, each of which maintains only a short length of the metal in the molten state. Many impurities are more soluble in the molten than in the solid metal, so that a succession of molten zones slowly sweeping along the ingot convey most of the impurities from one end to the other. The segregation constant of many impurities in germanium, i.e. the ratios of solubilities of impurities in solid and liquid germanium, have been determined by Burton and others;<sup>11</sup> for example, arsenic is given as 0.04, indium as 0.001, and boron as  $>1$ .

#### (3.4) Crystal Pulling<sup>12</sup>

When a single crystal is to be grown the metal is melted by high-frequency or other means, in vacuum or very pure inert gas, and during growth the temperature is maintained a few degrees above the melting point. A seed crystal with its end cut perpendicular to one crystal axis is lowered into the melt, to make fusion with the molten metal. The temperature is adjusted so that solidification commences at the junction of the seed and the melt, and the single crystal is raised a few inches per hour. Normally the crystal is rotated at about 100 r.p.m. to improve the homogeneity.

A number of subsidiary steps incidental to pulling the crystal may be mentioned.

##### (3.4.1) Resistivity Control.

Since the characteristics of the rectifier will be influenced by the resistivity of the crystal, some method of control is necessary. For example, a small percentage of impurity may be added to the charge of metal before melting, and the resistivity of the ingot will then vary along its length because of the increasing concentration of impurities in the molten metal due to segregation. However, by suitably matching the impurity concentration and pulling rate it is possible to effect a good measure of control along the crystal.

When making the fused-junction rectifier it is now customary to use an  $n$ -type crystal, and so the impurity to be added to the melt will consist of a very small amount of one of the elements having five valence electrons per atom, i.e. one more than the germanium or silicon atom. Such elements may be antimony and arsenic,

found in Group V of the Periodic Table. It follows that a  $p$ -type crystal could be made by adding impurity atoms with only three valence electrons, such as gallium or indium, found in Group III of the Table.

#### (3.4.2) Grown $p$ - $n$ Junctions.

A  $p$ - $n$  junction can be produced in a single crystal during growth by injecting pellets of  $p$ - and  $n$ -type impurities alternately into the melt and so producing  $p$ - and  $n$ -regions in the crystal;<sup>13</sup> Hall<sup>14</sup> describes an improved method in which the  $p$ - and  $n$ -impurities are already in the melt in suitable concentrations and are then segregated by rate-of-growth control, to form alternate  $p$ - and  $n$ -regions.

### (4) QUALITY CONTROL OF SEMI-CONDUCTORS

Physical and electrical analysis of the metal ensures that when passed for unit production it is consistent, i.e. free from pronounced lattice defects and conforming to certain standard electrical measurements.

#### (4.1) Physical Examination

Selected sections in a crystal may reveal strains and other defects in the lattice structure when the surface is suitably etched and examined optically. Fig. 3 illustrates defects in the lattice of a crystal which would render the material useless for large-area rectifiers. Such defects are usually caused by thermal or mechanical shock, and it is necessary to maintain very stable thermal conditions in the furnace during the period of crystal growth, which is from three to five hours, depending on the size of crystal. Mechanical shock is avoided by good design of the apparatus and care in operating it.

#### (4.2) Resistivity and Lifetime Measurements

For a full understanding of the need and purpose of measurements of resistivity and lifetime reference should be made to earlier publications, notably Valdes.<sup>15</sup> Suffice it here to say that they are required to appraise the quality of the metal and its suitability for the many types of device made from it. The resistivity of pure germanium at 20° C is about 65 ohm-cm, but the resistivity required for these rectifiers will be in the range 20–55 ohm-cm and the lifetime of their minority carriers should exceed 500 microsec, as shown in Fig. 4, which gives typical curves for these two parameters plotted against length of crystal. An improved method of lifetime measurement has recently been developed,<sup>16</sup> using a photoconductive decay technique, well suited to production analysis because it can be performed on whole crystals instead of on small sections, with a consequent saving of time and labour.

### (5) CUTTING TECHNIQUES

Before proceeding further with the discussion, different methods of cutting silicon and germanium into slices and wafers must be mentioned, because of their economic importance having regard to the high cost of single-crystal germanium and silicon.

The high-speed rotating-disc method for cutting quartz has generally been used for cutting germanium and silicon into slices and small wafers. However, it is wasteful when slicing ingots  $\frac{1}{2}$  in or more in diameter, because the wheel, impregnated with an abrasive dust, must be mechanically strong and thus may approach in thickness the slice to be cut. About 50% of the metal is therefore lost in the cutting sludge, and the cost of recovery is considerable. Where the depth of cut is small, much thinner wheels may be used.

A less wasteful method<sup>17</sup> of cutting is illustrated in Fig. 5. The machine employs a number of tungsten wires, of about

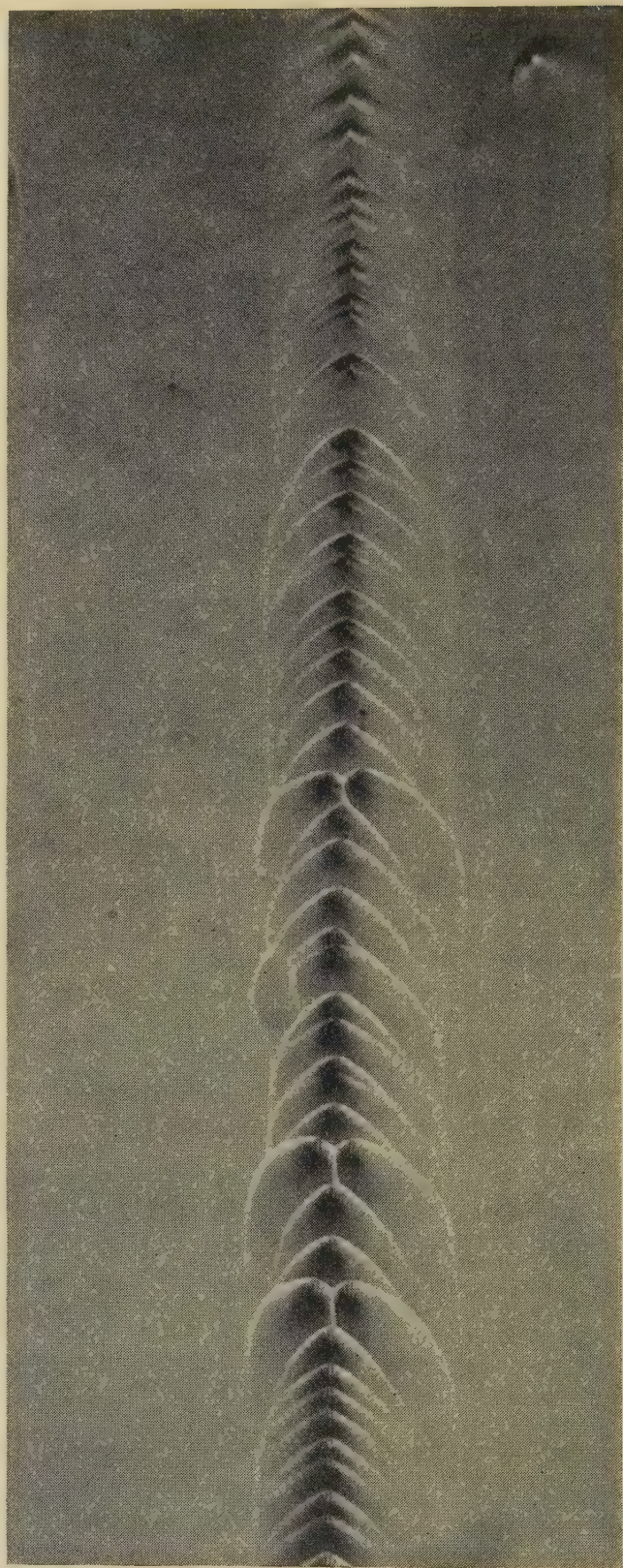


Fig. 3.—Lattice defects in a single crystal.

Etch pits on the surface of a single crystal, being the sites of the emergence of a row of edge dislocations at a tilt boundary.

Angle of tilt = 1–2 seconds of arc.  
Magnification =  $\times 210$ .

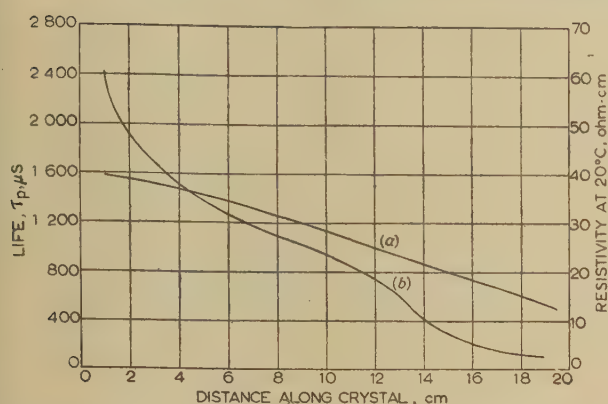


Fig. 4.—Resistivity and lifetime characteristics of germanium crystal.

(a) Resistivity.  
(b) Lifetime.

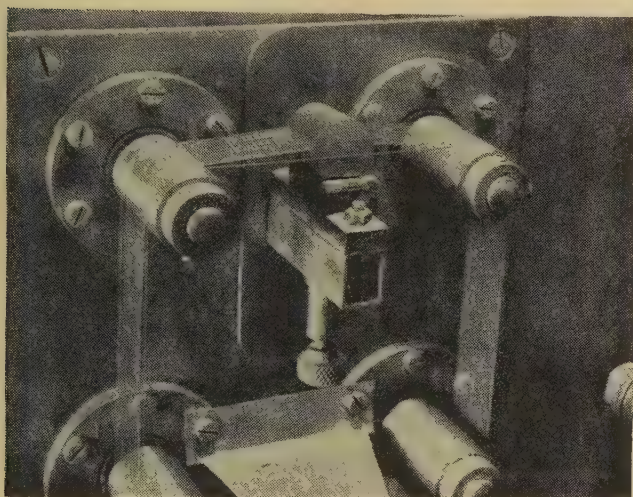


Fig. 5.—Method of slicing and dicing germanium crystal.

0.005 in diameter, loaded with fine abrasive dust in suspension. The wires are motor-driven backwards and forwards under tension against the metal, approximately one foot in each direction and reversing about three times per second. Up to 40 slices may be cut in an hour by this means.

#### (6) THE BASIC UNIT

The basic unit is illustrated in Fig. 2(b) and may be defined as an assembly common to all types of rectifier so far developed. The steps in its construction are as follows.

##### (6.1) Semi-Conductor

In general, the semi-conductor will consist of a wafer of *n*-type material about 0.02 in thick, with an area proportional to the current rating. The lower surface is soldered intimately to a base material having about the same coefficient of expansion as the wafer. The base connection must also provide low ohmic resistance and good thermal conductivity.

##### (6.2) Fusion of Impurity to Semi-Conductor

For germanium, pure indium is a convenient acceptor element for fusing to the wafer. A pellet is applied to one surface and then fused in an inert atmosphere. As the oven temperature

is increased to about 600°C the indium will dissolve germanium until a saturated solution is obtained. Fig. 6 shows a typical phase diagram of the fusion system. The amount of germanium dissolved will depend upon the volume of indium and the furnace

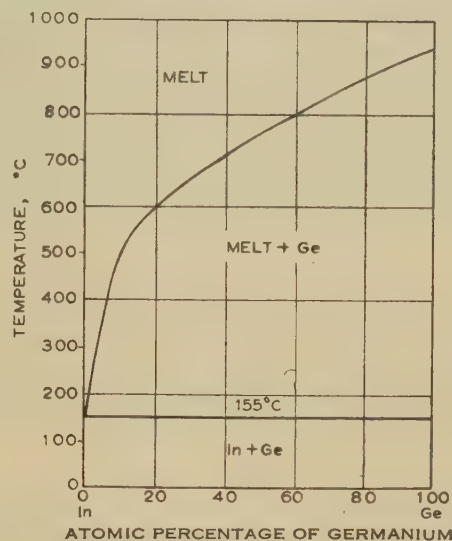


Fig. 6.—Phase diagram of indium-germanium system.

temperature, suitable adjustment of these ensuring satisfactory control of the thickness of the *n*-region and hence of the forward characteristic—an essential requirement in production to ensure uniformity of characteristics for parallel operation. When fully saturated the indium will diffuse some small distance into the unmelted germanium, and will thus determine the concentration gradient at the junction.<sup>18</sup> On cooling, the germanium will precipitate out of solution to form a rich *p*-layer of heavily impregnated crystals.

#### (6.3) Etching the Basic Unit

A shallow depression around the fused layer, as shown in Fig. 2(b), is formed by electrolytic etching after the *p-n* junction is made. This step removes traces of indium or other impurities which may bridge the junction on the surface. The junction area must then be protected from moisture and other foreign matter in the interval before final sealing in its container.

#### (7) TYPES OF FUSED *p-n* JUNCTION RECTIFIERS

A number of experimental types were first made in 1951, and production units rated at about 40 watts were available in 1952. Units with ratings of about 2 kW were also developed, and these have now reached the production stage. Fig. 7 illustrates a range of units, and their characteristic curves are shown in Fig. 8, while Table 1 gives some operating and other data for the units produced early in 1955.

##### (7.1) Common Constructional Features

Since many of the earlier types are now of historical interest and most production types differ only in detail, the essential features common to all types, regardless of rating, can best be described by reference to the sectional diagram Fig. 9(a). This construction<sup>19</sup> is designed primarily for natural or forced air cooling when suitably mounted on some heat-radiating structure (not shown). The metal container is hermetically sealed to the base and all seals must withstand prolonged periods of tropical

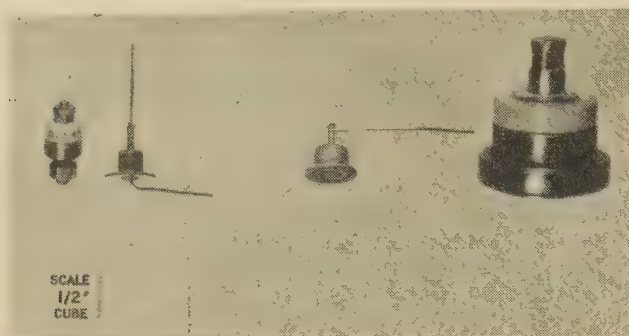


Fig. 7.—Germanium rectifier units without fins.

Left to right:—Low-, medium- and high-power types.

test. The terminal connections must not strain the rectifier mechanically, and the temperature difference between the  $p$ - $n$  junction and the heat radiator attached to the base must be as small as possible. Fig. 9(b) shows a type of flexible connection which facilitates the assembly and exerts no mechanical strain on the rectifier.

### (7.2) Germanium Rectifiers

Three types of rectifier are identified in Table 1 under the headings low, medium and high power respectively. Other parameters and the cooling means which determine the rating are also given.

Each type has a number of grades corresponding to different peak inverse voltages (p.i.v.) and the two similar low-power types differ only in the manner of construction.

#### (7.2.1) Common Electrical Characteristics of Germanium Rectifiers.

In all types the low voltage drop in the forward direction is a common feature and does not change for different grades in a given rating, nor appreciably with change of temperature over the working range.

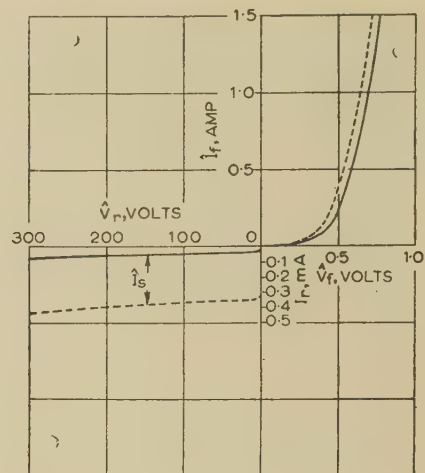
The inverse characteristics of the various types are also similar in form but differ in other respects. There are two parts of the inverse characteristic to be considered: the saturation region where the current is nearly constant with voltage but variable with temperature, and the breakdown region where a small increase in voltage produces a large increase in current. This inverse breakdown phenomenon depends upon the special properties of the semi-conductor correlated with inverse current and voltage, as discussed more fully in Section 14.2.

Another common feature is the hole-storage effect, which sets an upper limit to the frequency which can be used with germanium and silicon devices. However, in power rectifiers this limitation is not so important as the transient voltages which may occur under some operating conditions at normal supply frequencies.<sup>20</sup>

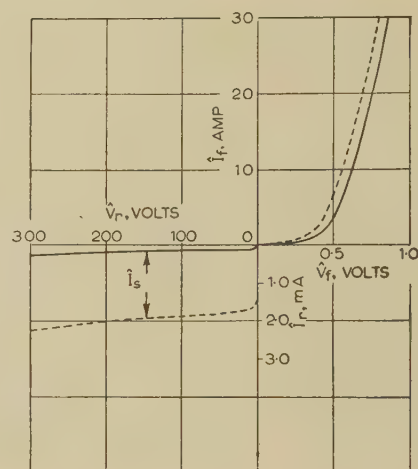
All these phenomena are described in the literature, but since the power rectifier is a special case additional information on them is given in Section 14, including another theory of current flow in a fused  $p$ - $n$  junction rectifier.

### (7.3) Silicon Rectifiers

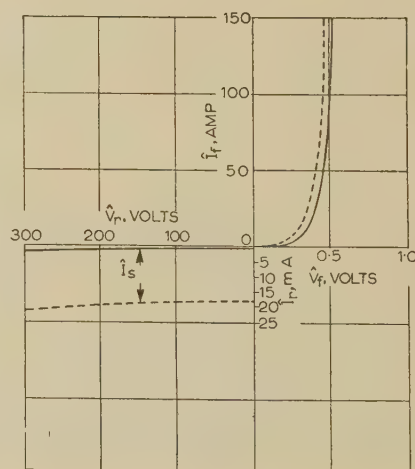
Since the effect of thermal agitation on the electrons in silicon is less than in germanium, because of certain physical differences between them, the reverse current is very much smaller for a given temperature, and therefore it has not the same limitation when operating at high ambient temperatures. Although one of the common elements, silicon presents a greater problem in its production in the refined state required, because of its higher melting point and susceptibility to contamination. However,



(a)



(b)



(c)

Fig. 8.—Typical 50 c/s characteristics of germanium rectifiers at various ambient temperatures.

(a) Low power. — 25°C, --- 55°C. (b) Medium power. — 20°C, --- 55°C. (c) High power. — 25°C, --- 70°C.

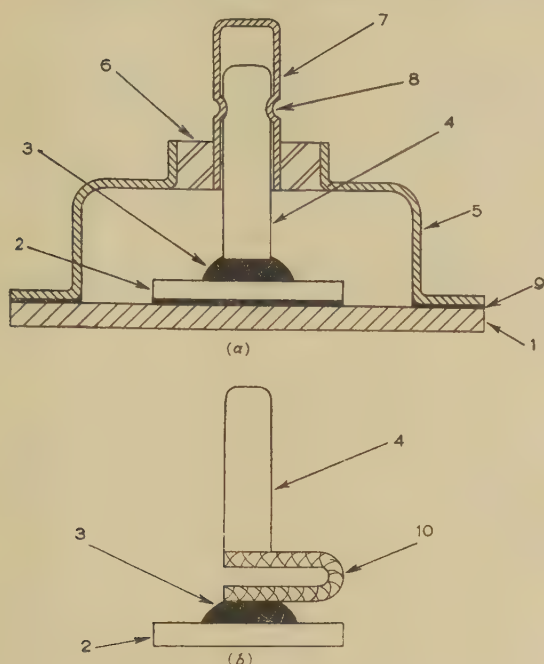


Fig. 9.—Section of rectifier assembly.

1. Base.
  2. Germanium wafer.
  3. Indium.
  4. Copper electrode.
  5. Metal body.
  6. Insulator.
  7. Upper terminal.
  8. Crimp.
  9. Hermetic seal.
  10. Flexible connection.
- (a) Normal connection.  
(b) Alternative connection.

power rectifiers with ratings of more than 1 kW have already been reported.<sup>21</sup>

Fused-junction types have been made by one organization<sup>22</sup> and are similar in appearance to corresponding germanium types. Characteristic curves are shown in Fig. 10.

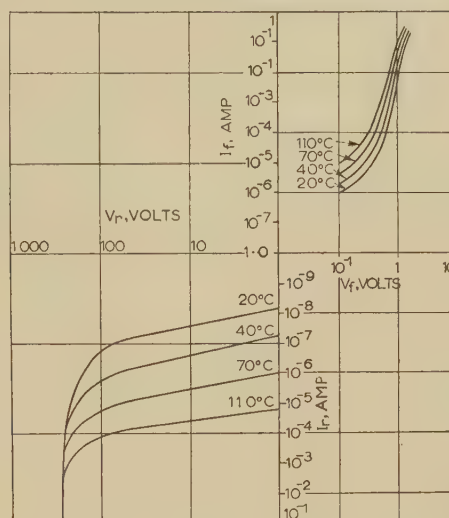
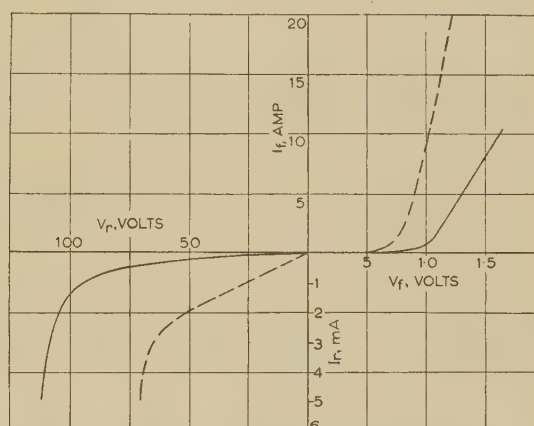
Fig. 10A.—Typical characteristics of small-area (0.1 mm<sup>2</sup>) silicon junction rectifier.Fig. 10B.—Characteristics of silicon power rectifiers at 20° C.  
- - - 20 mm<sup>2</sup> junction. — 5 mm<sup>2</sup> junction.

Table 1

TYPES OF GERMANIUM POWER RECTIFIERS

Type	Power rating	Approximate area of junction	P.I.V.	Mean load current		Forward voltage drop	Fins	Cooling
				at 35° C	at 55° C			
GJ4D	rating	mm <sup>2</sup>	volts	amp	amp	volts		
GJ1E	Low	1	100	0.25 0.5	0.1 0.2	0.5 at 0.2 amp	Without With	Natural
GJ3D	Low	1	200	0.25 0.5	0.1 0.2	0.5 at 0.2 amp	Without With	Natural
GJ5D	Low	1	300	0.25 0.5	0.1 0.2	0.5 at 0.2 amp	Without With	Natural
GJ1F	Medium	10	50	5	2.5	0.5 at 3.5 amp	With	Natural
GJ2F	Medium	10	100	5	2.5	0.5 at 3.5 amp	With	Natural
GJ3F	Medium	10	150	5	2.5	0.5 at 3.5 amp	With	Natural
GJ4F	Medium	10	200	5	2.5	0.5 at 3.5 amp	With	Natural
GP1B	High	20	100	20	—	0.5 at 60 amp	With	Natural
GP2B	High	20	200	20	—	0.5 at 60 amp	With	Natural
GP1C	High	64	25	60	—	0.5 at 100 amp	With	Forced air
GP2C	High	64	50	55	—	0.5 at 100 amp	With	1000 ft/min
GP3C	High	64	100	50	—	0.5 at 100 amp	With	

## (8) RATING OF JUNCTION RECTIFIER

As can be seen from the characteristic curves, Fig. 11, the internal inverse power loss increases rapidly with temperature.

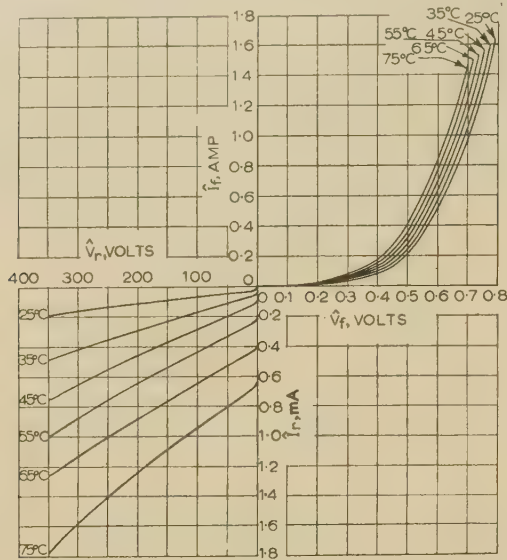


Fig. 11.—Typical lower-limit characteristics of type GJ5D rectifier.

It therefore follows that this must not increase faster than the heat flow from the junction, otherwise thermal breakdown will result. The condition of thermal stability may be obtained from the equation:

$$V_r \frac{\partial I_s}{\partial \theta_j} < \frac{1}{S} \quad (1)$$

in which  $S$ , the total thermal resistance of the rectifier, is defined as the rise in junction temperature in degrees centigrade above ambient per watt dissipated in the unit.\*

## (8.1) Maximum Peak Inverse Voltage

With high-voltage germanium rectifiers the maximum peak inverse voltage is set by thermal considerations rather than by Zener breakdown. Assuming the rate of increase of inverse current with temperature to be 7% per deg C, we have, for half-wave a.c. operation,

$$0.318 \hat{V}_r < \frac{1}{S} \times \frac{14.3}{I_s} \quad (2)$$

This gives values of  $\hat{V}_r$  much higher than can be used in practice, because the rate of increase of inverse current is increased by the incidence of current multiplication at high voltages, as further discussed in Section 14.2. An empirical method of rating rectifiers for maximum peak inverse voltage must therefore be used, e.g. by noting the peak inverse voltage for a given inverse power dissipation at maximum junction temperature and allowing some factor of safety to ensure reliability. For example, eqn. (2) will give  $\hat{V}_r < 720$  volts for a rectifier with an inverse saturation current of 0.5 mA at 75°C and a thermal resistance of 125°C/watt. This corresponds with the characteristics of the GJ5D rectifier where, however,  $\hat{V}_r$  is rated at only 300 volts for the above reasons.

At junction temperatures lower than the maximum permissible value, a higher maximum peak inverse voltage may be used, and this must be found by another inverse power test at the lower temperature.

\* A similar method for testing transistors has been used in the United States by J. S. Saby.

## (8.2) Maximum Junction Temperature

In the early stages of development it was customary to limit the junction temperature of germanium rectifiers to 75°C, and all ratings were based on this figure. Recent experience, however, has shown that, with improved techniques in manufacture, operating temperatures of up to 100°C are now possible. However, the maximum peak inverse voltage at 100°C must of necessity be less than when working at a lower temperature.

Similar considerations will apply to the silicon rectifier, except that its maximum junction temperature may be raised to about 300°C; but at lower temperatures the maximum peak inverse voltage will be limited by Zener, rather than by thermal, breakdown.

## (8.3) Thermal Resistance

The thermal resistance of the rectifier has two components, internal and external, the latter including that of the cooling system (usually consisting of fins for natural or forced air cooling, or some other arrangement if liquid or vapour cooling is adopted). The cooling system is dependent, *inter alia*, on the heat generated by the unit, as discussed later.

In the measurement of the internal thermal resistance use is made of the relation between the inverse saturation current and junction temperature. A rectifier is first calibrated in an oven by plotting the saturation current against temperature. A known d.c. power is then applied to the rectifier in the forward direction and interrupted at intervals by short inverse voltage pulses, during which the saturation current is measured. The timing of the measurement must be arranged to exclude any component of the hole-storage current associated with the reversal of voltage. Then, from the oven calibration the junction temperature rise may be obtained for a known power dissipation.

## (8.4) Method of Rating

The rating of a rectifier may be expressed by the equation

$$\theta_j = \theta_a + SP \quad (3)$$

For normal a.c. operation,  $P$  includes the forward and inverse power developed in the unit, and these losses may be calculated from the characteristic curves. Thus, two sets of curves may be drawn: Fig. 12(a) giving forward power loss plotted against mean forward current, and Fig. 12(b) giving inverse power loss plotted against peak inverse voltage for the range of temperatures specified in each case. Given  $P$  from eqn. (3) and the peak inverse voltage for a given application, the inverse power loss is determined from Fig. 12(b) and this when subtracted from  $P$  gives the forward power loss, from which the permissible mean forward current may be obtained.

Consider the lower limit characteristic of a GJ5D rectifier (Fig. 11). With a maximum junction temperature of 75°C and a thermal resistance of 125°C/watt the allowable mean current for various ambient temperatures may be obtained by reference to Fig. 12. For an ambient temperature of 55°C the total allowable power loss is 160 mW with a peak inverse voltage of 300 volts. From Fig. 12(b) the inverse power loss at 75°C is found to be 110 mW, leaving 50 mW for the forward loss. From Fig. 12(a) this gives a maximum mean forward current of 115 mA.

Similarly, at an ambient temperature of 35°C and with a peak inverse voltage of 300 volts, the total allowable loss is 320 mW; there is 210 mW left for the forward loss, allowing a mean current of 320 mA.

Also, at the same ambient temperature but with a peak inverse voltage of only 24 volts, we have 314 mW left for the forward loss, giving a forward current of 450 mA. The low-power type has been used simply to illustrate the method of

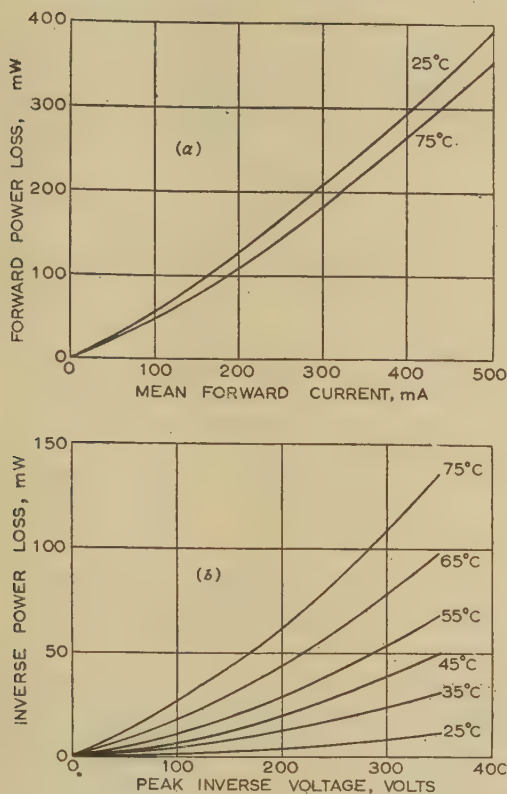


Fig. 12.—Power loss curves for type GJ5D rectifier.

(a) Forward.  
(b) Inverse.

from those on low- and medium-power units in other respects, as will be seen from the description.

#### (9.2.1) Forward Voltage Drop.

This measurement checks the variation in forward resistance expressed as a voltage drop. It must not exceed  $\pm 6\%$  of a standardized value for those units required to be used in a parallel connection, otherwise current sharing will be unequal and some units will be overloaded.

#### (9.2.2) Accelerated Life Test.

Although this is a relatively short test, it is effective in eliminating units having unstable characteristics. The junction of the unit is heated and cooled intermittently over a period of several hours by the passage of full-load current at a low voltage.

#### (9.2.3) Voltage Grading.

Rectifiers are classified into a number of grades for each rating based on the peak inverse voltage. The tests are made at 70°C with a 50 c/s voltage which is gradually increased to a value considerably higher than the rated figure. The inverse power loss must not exceed a specified value at this temperature.

#### (9.2.4) Load Test.

Each rectifier is tested in a 3-phase bridge with the peak inverse voltage and mean forward current adjusted above the rated value, thus simulating operating conditions more severe than normal. A single-phase bridge may also be used if the load has sufficient inductance to reduce ripple in the d.c. output to about the same value; it must also operate with a peak inverse voltage about 20% higher than the 3-phase test, in order to compensate for the longer duration of inverse voltage, i.e. 210° instead of 150°.

The inverse current is measured before and after this test; any unit showing a change of characteristic is rejected.

#### (9.2.5) Fault-Current Tests.

To ensure that each rectifier will withstand the conditions arising when the equipment sustains a short-circuit on the d.c. side, each unit is subjected to this test in a single-phase circuit in which one unit is short-circuited and the resultant current surge through the other is interrupted electronically, after a preset number of cycles. A typical overload requirement would be for the unit to withstand eight times its normal load current for half a cycle.

#### (9.2.6) Ageing and Life Tests.

There is no evidence of an adverse ageing effect in the  $p-n$  junction rectifier when precautions have been taken to protect the junction from chemical attack. Deterioration and subsequent breakdown is invariably caused by residual chemical contamination, often accelerated by the presence of moisture trapped in the unit or allowed to penetrate through the seals—in short, defective design or processing.

The most practical way of checking these effects is obviously by life testing under normal working conditions. Installations have now been in continuous operation for nearly two years without ageing effects being observed.

### (10) APPLICATIONS

It is not possible within the scope of the paper to describe all the existing and novel applications for such a range of rectifiers. It will suffice briefly to mention their chief advantages over existing types and then to mention a few typical installations and their modes of operation. High conversion efficiency and

### (9) ELECTRICAL TESTS

The object of the electrical tests is to ensure that each unit complies with its specified rating and to test the permanence of the electrical characteristic; but since this can be satisfied only by a life test, complete data on these newly developed devices are not yet available. However, several tests have been devised for selecting and grading the units.

#### (9.1) Low- and Medium-Power Types

The tests applied to low- and medium-power units consist of a forward-voltage-drop measurement and an inverse voltage grading. An alternating voltage is applied to the rectifier and the peak forward voltage across it is measured for the rated mean forward current, with an upper limit set for this peak voltage at room temperature. In grading, the peak inverse voltage is determined for a given inverse power dissipation when the junction temperature is raised to the maximum permissible value. In this measurement a high alternating voltage is applied to the rectifier through a series limiting resistor of such value as to ensure a substantially constant power dissipation in the rectifier, independent of its inverse resistance. The peak inverse voltage across each rectifier is noted, and the unit is graded in one or other of the several voltage ratings, as shown in Table 1.

#### (9.2) High-Power Types

The need for extreme reliability in large power equipment requires somewhat more stringent and more comprehensive tests to be made on each unit. The tests on high-power units differ

inherent reliability, coupled with small overall dimensions and weight, are common features of all types. Other factors such as relative costs and availability must also be considered.

### (10.1) Low- and Medium-Power Types

These types have already been established in such applications as small battery chargers, power supplies for electronic and radio equipment, low-voltage d.c. supply systems, especially in aircraft or similar locations where small volume and low weight are of particular importance.

#### (10.1.1) Series and Parallel Connections.

In the selection of units for series operation there must be a substantially equal sharing of inverse voltage from the lowest to the highest working temperature, and unless units have similar characteristics it is safer to connect high-value resistors in shunt with each rectifier as may be recommended by the manufacturer. Similarly, in the absence of some selection a small current-equalizing resistor may be connected in series with each rectifier where the units are connected in parallel.

#### (10.1.2) Circuit Precautions.

Since the forward resistance of these rectifiers is very small it is desirable to use transformers and smoothing chokes with correspondingly low-resistance windings in order to obtain the best possible regulation. It follows that the system must be adequately protected from the effect of short-circuits by the inclusion of fuses or other means.

The low circuit resistance also demands some protection in other respects. If, for example, a reservoir capacitor is used, the peak charging current should be limited, if necessary, to the surge rating of the rectifier by the insertion of a low-value resistor either in series with the capacitor or in the main circuit. The effect on the performance will be practically negligible whichever position is chosen.

### (10.2) High-Power Types

The germanium power rectifier combines the high efficiency of the contact rectifier with the static advantage of the mercury-arc rectifier. In addition, it can cover a wider range of voltage and power ratings.

Other early converting devices such as hot-cathode, hard-vacuum and metal rectifiers have each found profitable employment but are now faced with formidable rivals, and it is interesting to compare the growing array of converting equipment with other types of apparatus such as transformers, d.c. motors, or alternators, where the main principle of operation has remained unchanged for half a century. Although notable advances in performance and size of such apparatus have been made, their designers have been spared continual changes of type.

#### (10.2.1) Circuit Connections.

For small powers a single-phase half-wave or a full-wave rectifier can be used, but the output voltage contains a large ripple. The simplest arrangement for rectifiers of more than 1 or 2 kW is the 3-phase bridge connection shown in Fig. 13. Current flows to the positive d.c. connection from whichever terminal of the transformer secondary winding is the most positive at the time, and it flows back from the negative d.c. connection into whichever terminal is most negative. Thus the outgoing current flows in turn through the top three rectifier units, each for one third of a cycle, and back through the bottom three. The d.c. positive terminal has the potential of whichever terminal, A, B or C, is the most positive, and the d.c. negative terminal that of whichever terminal is the most

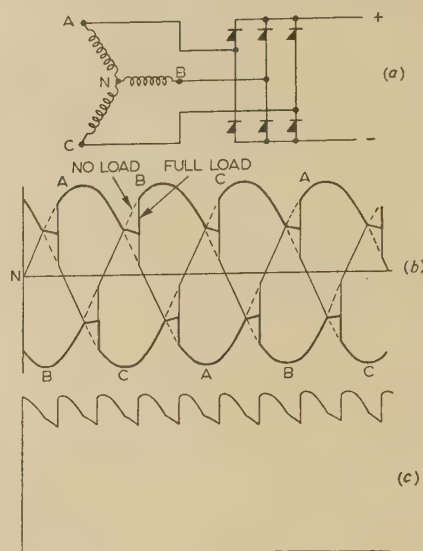


Fig. 13.—Normal rectifiers in 3-phase bridge.

- (a) Connection diagram.
- (b) Line-voltage waveforms.
- (c) Direct-voltage waveforms.

negative. The direct voltage is thus the difference between the two thickened lines and is shown in the second curve. It has a ripple of about 6% at a frequency of six times the supply frequency, i.e. 300 c/s on 50 c/s supplies. For just over one third of each cycle each unit carries forward current; for the remainder it is subjected to a reverse voltage having a peak value equal to the peak direct voltage.

If each unit can carry a mean current of 50 amp the d.c. output from the bridge is 150 amp. If each unit can withstand a peak inverse voltage of 100 volts the d.c. voltage will be 100 volts peak and about 90 volts mean. The output power will then be 13.5 kW, or 2.25 kW per unit.

It is worth noting that the transformer windings carry only alternating current with no d.c. component. Thus the rating of the secondary windings is reduced to 70% of that required for a multi-anode mercury-arc rectifier. The winding also becomes more simple than the 6-phase winding needed for a mercury-arc rectifier. This fact provides some welcome relief for the transformer designer.

The simplification of the transformer, however, is accompanied by some disadvantages: the connections of the rectifier compared with those of a 6-phase double-star-connected rectifier are more complicated, and involve more fuses and more copper in the busbars; the arrangement of the rectifier units in twice as many series strings, having only half the inverse voltage across them, gives less freedom in the choice of the number of units in series. These factors make it desirable to use the double-star connection in some cases, particularly where the d.c. output is a high current at a low voltage, as in electroplating.

The harmonic currents produced in the a.c. supply system by a 3-phase bridge rectifier rated at several hundred kilowatts may sometimes cause interference in telephone circuits. In such circumstances it is necessary to cancel the harmonics of lowest frequency, i.e. the fifth and seventh harmonics, by using two 3-phase bridges displaced in phase by 30° from each other to give what in mercury-arc-rectifier language would be called 12-phase operation. This can be done by the use of one star and one delta transformer-secondary winding. For still larger equipments it is necessary to use a larger number of 3-phase bridges, displaced in phase from each other, to give 24-, 36-, 48- or 60-phase operation.

## (10.2.2) Overlap and Hole Storage.

Owing to the reactance of the transformer windings, current cannot be transferred instantly from one phase to another; the waveform of the current through a unit is therefore as shown in Fig. 14, the period PQ being the time taken for the current to

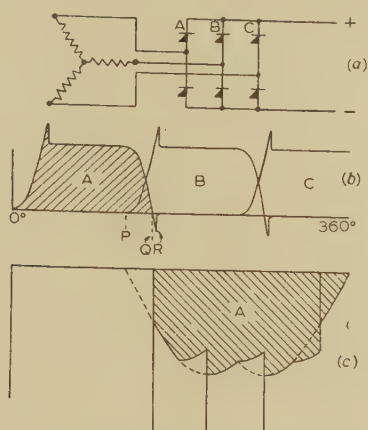


Fig. 14.—*p-n* junction rectifiers in 3-phase bridge.

- (a) Connection diagram.  
(b) Current waveform.  
(c) Waveform of voltage across one unit.

fall to zero in unit A and to rise to full value in unit B. Because of the hole-storage effect, however, the current does not stop at zero but overshoots in the reverse direction for a few degrees and then is abruptly reduced to zero at point R. On the assumption that the output current is constant, unit B, which is taking over the current from unit A, must have an opposite pulse of current at the same moment. These pulses appear in the current of each unit.

The rapid decrease of current when the hole storage ends produces a sharp spike in the waveform of voltage in the circuit through which the current flows, and these spikes, occurring every sixth of a cycle, are shown on the inverse voltage across unit A in Fig. 14(c); the two which occur during the conducting period of unit A appear in the inverse voltage of other units. These high voltages, although of very short duration, can damage a rectifier even if they do not exceed the normal breakdown voltage, since the breakdown voltage is lower immediately after the passage of forward current; they are unlikely to damage the transformer windings. The voltage oscillations which follow the spikes can cause limited interference to radio reception. These undesirable effects can be reduced to harmless proportions by connecting across the a.c. terminals of the bridge, capacitors into which the reverse current can flow when it is abruptly rejected by the rectifiers.<sup>23</sup> Fig. 15 shows an oscillogram of the current in a unit and the voltage across it when working in a circuit without these capacitors.

## (10.2.3) Series and Parallel Operation.

When the direct voltage required is higher than can be obtained from a simple 3-phase bridge it is necessary to connect units in series. Owing to the variation of the characteristics between units, the reverse voltage will not be divided evenly between them. To correct this it is possible, as previously suggested, to connect across each unit a resistance which is low compared with the lowest reverse resistance of any unit. However, the power loss in the resistors will then be large compared with the reverse power loss in the units, and the overall efficiency will be reduced. A capacitor connected across each unit would give some equality of reverse voltage but would be costly. If it were

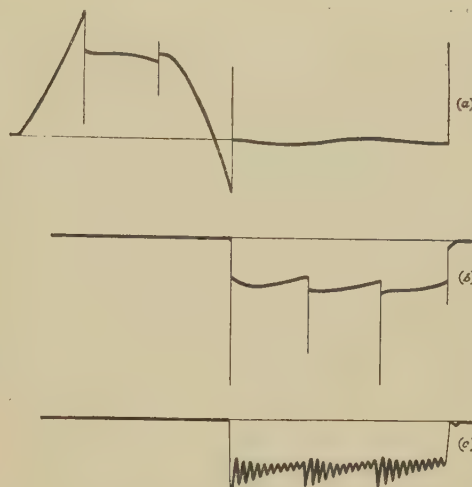


Fig. 15.—Oscillograms of a 3-phase bridge rectifier.

- (a) Current in a rectifier unit.  
(b) Voltage across the unit.  
(c) Voltage after fitting capacitors.

true that all units failed at the same reverse voltage it would be best to equalize their reverse voltages. However, it is more nearly true that they fail at the same reverse current, and the best way of connecting units in series in high-power equipment has been found to be simply to connect them in series.

For currents greater than three times the unit rating, units or strings of units must be connected in parallel. The forward voltage drop varies slightly between units, but to equalize the currents in the parallel paths by means of series resistors lowers the efficiency, and to do so by series reactors is cumbersome. The best solution for high-power operation is to reduce the average current slightly so that no unit is overloaded.

## (10.2.4) Cooling.

The loss per 50 amp rectifier unit is 25 watts forward and 2 or 3 watts reverse, i.e. about 30 watts total, and this amount of heat must be removed from the base of the unit. In order to give a reasonable margin below the temperature of 70°C at which the unit has been tested, the temperature of the base should not exceed 55°C. For small equipments using only a few rectifier units, the most satisfactory method of cooling is by natural convection, the units being provided with sufficient cooling surface, finned or otherwise, to maintain a suitable temperature.

For large equipments in temperate countries, where the air temperature does not often exceed 30°C, the heat can easily be removed by air drawn past fins attached to the base of the unit, the units and fins being arranged on trays in a cubicle. The space occupied by the fins is small, the fan can be driven by a squirrel-cage motor, which is very reliable, and the power consumed by the fan is only about 0.1% of the rectifier output.

In tropical countries, where the air temperature may be 50°C, or in locations where the air contains dirt or corrosive gases and is unsuitable for passing through the rectifier cubicle, some form of water cooling is necessary. Each rectifier unit may be provided with a passage for water, but a large number of units at different potentials and connected by water pipes is not attractive. A better arrangement is to use air cooling and to cool the air in either a closed or an open circuit with a water-cooled heat exchanger. If mains water is used it must usually be paid for and a reserve tank provided to avoid shut-down of the rectifier due to water failure. It frequently happens abroad that the water is too corrosive or too hard to pass directly through

cooling passages, and a water-to-water heat exchanger must be used.

Another proposed method of cooling is to surround the unit with a volatile liquid such as carbon tetrachloride in an hermetically sealed vessel at such a pressure that the liquid is just below its boiling point; a small rise in temperature of the unit then produces bubbles of vapour which rise to the wall of the vessel, where they condense and give up their heat; this is removed by air or water cooling on the outside of the vessel. If the liquid is in direct contact with the unit the choice of a suitable insulating liquid is difficult; if it is not in direct contact some of the advantage of the method is lost. The purpose served by the liquid is to transfer the heat from the unit to the ultimate cooling medium with the minimum temperature drop, but this can be done almost as well with a solid-metal heat path. Vapour cooling, however, has also the advantage of insulating the basic rectifier from the cooling system.

It is possible also to cool and protect the sealed and finned units by immersing them in oil in a tank provided with cooling tubes, as on a transformer. This method is very useful for small equipments in bad atmospheric conditions, but for large equipments it occupies considerable space, unless the cooling tubes are cooled by water or forced draught.

#### (10.2.5) Efficiency.

The power loss occurring during the reverse part of the cycle is small compared with the forward loss, which is itself small. The reverse loss can therefore be neglected and the efficiency calculated from the forward loss only. The forward voltage drop at full load is approximately 0.5 volt; in the bridge connection the current flows through two units in series, making a total drop of one volt. If the peak inverse voltage across a unit is limited to 100 volts, corresponding to a full-load direct voltage of 90 volts, the loss is 1 volt in 90, or 1.1%. Connections, cooling fan and fuses account perhaps for a further 0.5%, giving a total loss of 1.6%. The loss in the main transformer for a 1 MW equipment would be about 1.4%. The overall efficiency of the equipment would therefore be 97%. Since the only losses occurring at no load are the transformer core loss and the fan loss, the efficiency is high down to 10% of full load.

The very low voltage drop in a germanium rectifier allows a high efficiency even at very low output voltages. Fig. 16 compares the efficiency plotted against direct output voltage for the various methods of conversion for an output of 15 kA. It will be seen that even at 10 volts d.c. the germanium rectifier equipment has an overall efficiency of 90%. Copper-oxide and selenium rectifiers have a much lower efficiency. Germanium rectifiers thus provide a means of obtaining low direct voltages at high efficiency where no means previously existed.

#### (10.2.6) Voltage Control.

At present, germanium rectifiers have no means of voltage control comparable with the grid control of a mercury-arc rectifier. The direct voltage can, however, be controlled by varying the alternating input voltage to the rectifier by one of three main methods. First, by transformer tapings, which can be of the off-load type for infrequent operation, or the on-load type where operation is more frequent or where automatic control is required. Secondly, for smooth stepless variation a voltage regulator can be interposed on either the primary or the secondary side of the transformer; for reasonably frequent operation this can be a transformer-type regulator with a contact moving over the winding; for very frequent operation an induction regulator is preferable, but is more costly. Thirdly, for automatic control of voltage with almost continuous variation a saturable reactor may be interposed on either the primary or

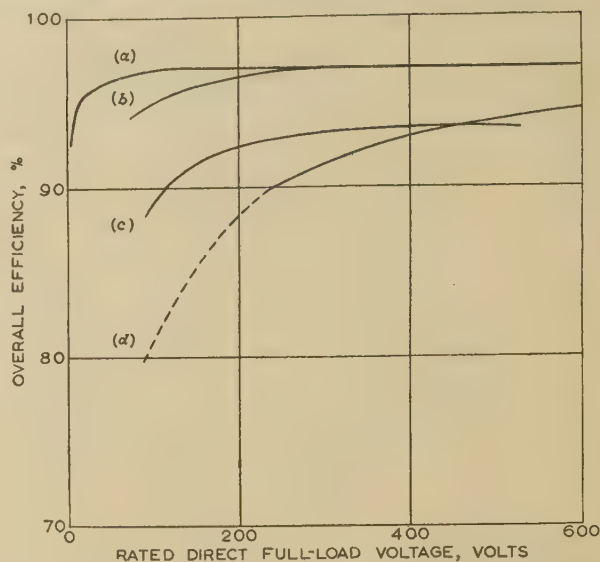


Fig. 16.—Efficiencies of converters for 15 kA operation.

- (a) Germanium rectifier.
- (b) Contact rectifier.
- (c) Motor converter.
- (d) Mercury-arc rectifier.

the secondary side of the transformer; this method gives smooth and rapid control, but since it reduces the power factor in the same ratio as it reduces the direct voltage, it must be used in moderation and supplemented by transformer tapplings if a wide range of control is required.

By the use of one or more of these methods any voltage-control requirement can be met, and although the solution may not be so elegant as grid control, the waveform will not be so sharp-toothed.

The one thing which cannot be achieved by germanium rectifiers is the conversion from d.c. power to a.c. power. For this we must await future developments.

#### (10.2.7) Comparison with Earlier Types.

Where the cost of electrical power is a major item, as, for example, in electrolytic processing equipment, an improvement in efficiency of a fraction of 1% may save thousands of pounds per annum, and germanium is the obvious choice. Moreover, during the past 20 years the operating voltage of electrolytic-cell lines has been raised from about 200 to 600 volts and even 1 kV, in order to use mercury-arc rectifiers at a more efficient voltage. Since germanium rectifiers give their maximum efficiency at any voltage from about 70 volts upwards, a low cell-line voltage will now become more practicable; this would ensure greater safety for the operating staff and in some instances permit the use of larger individual cells with an improved performance.

For applications such as electroplating, requiring large currents at low direct voltages of 5–50 volts, the efficiency of the germanium rectifier is so much higher than that of any other type of equipment that there seems no reasonable alternative to it.

For small d.c. power supplies of from 1 to 100 kW the question of efficiency is not so important, but the germanium rectifier is so much smaller in bulk than copper-oxide and selenium rectifiers that it seems likely to be used extensively.

A mercury-arc rectifier can deliver nearly as much current at 600 volts as at 200 volts, so that its cost per kilowatt decreases rapidly as the voltage increases. A germanium rectifier is made up of a number of units in series and parallel and the cost per

kilowatt is more nearly constant. There will therefore be some direct voltage below which the germanium rectifier will be cheaper and above which the mercury-arc rectifier will have the advantage, but this level is not yet known.

#### (10.2.8) Equipments in Operation.

In December, 1953, a 300 kW germanium rectifier was installed in the lamp works of a British manufacturer to supply d.c. power to electrolytic cells for hydrogen production. It replaces a grid-controlled glass-bulb mercury-arc rectifier and operates from the rectifier transformer previously used. It occupies one-sixth of the space taken by the glass-bulb rectifier, has an efficiency 6% higher and makes a saving of £700 per year on power. It has now been in service for more than 10 000 hours. This was the first germanium power rectifier to be installed in this country, and, so far as is known, the first of this rating in the world.

Fig. 17 shows the interior construction, with the rectifier units mounted on withdrawable racks and the cooling fan at the top to draw air upwards over the units. The a.c. leads from the transformer enter at the back and current flows down through

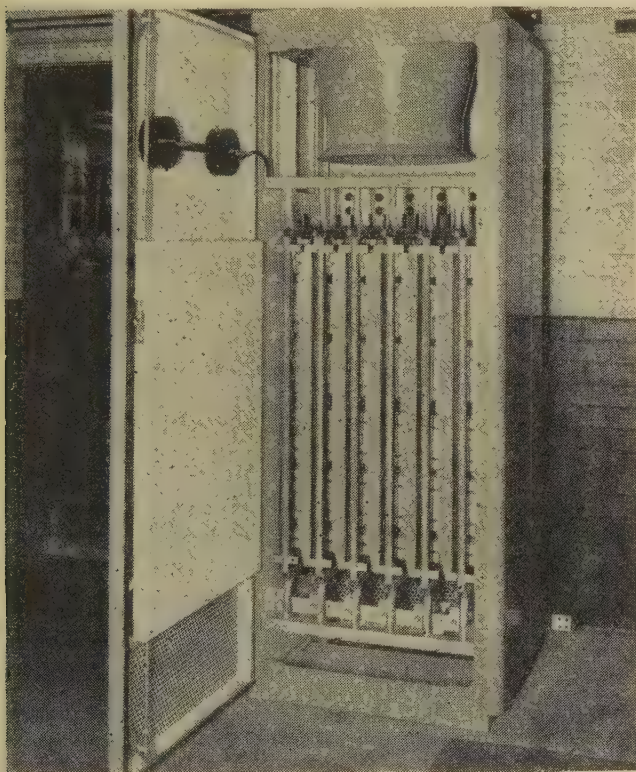


Fig. 17.—300 kW germanium rectifier assembly in cubicle.

the units to the d.c. busbar at the bottom. A relay mounted above the fan is connected to trip the d.c. circuit-breaker if the fan stops.

Fig. 18 shows one of the rectifier units from this equipment, with and without its cooling fins. Each unit has a d.c. rating of 50 amp.

Another 300 kW rectifier has also been installed in a factory attached to the same organization; it provides a general power supply at 500 and 250 volts to a 3-wire d.c. system, and also operates from an existing transformer. The rectifier cubicle is only about one-half the size of the one previously

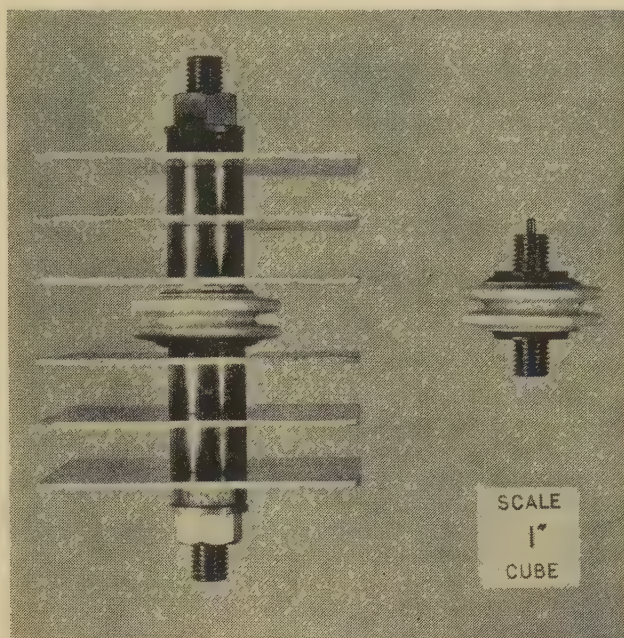


Fig. 18.—Germanium rectifier units with and without fins, used in 300 kW equipment.

mentioned, although in this instance it contains fuses, reactors, current transformers and relays. This equipment has been in service for more than a year. An equipment rated at 1 MW at 255 volts was put into continuous service in August, 1955, supplying current to an electrolytic-cell line. This equipment has two rectifier cubicles each 3 ft square and 7 ft 6 in high; the simplified connection diagram is shown in Fig. 19. An

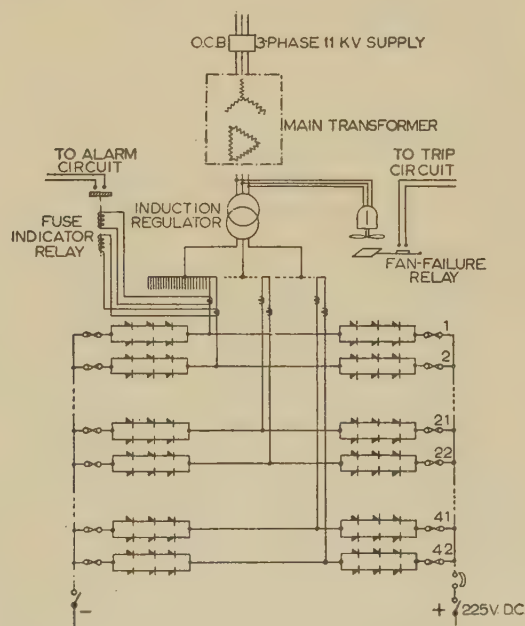


Fig. 19.—Connection diagram of 1 MW germanium rectifier.

18 MW equipment is now being built to supply power to two 30 kA electrolytic-cell lines, and is the largest germanium installation on order in any part of the world.

## (11) CONCLUSIONS

It is anticipated that increasing use will be made of the  $p$ - $n$  junction rectifier as a power convertor with a demand for units having higher current and voltage ratings. Single germanium units with current ratings exceeding 100 amp have already been made experimentally, and it is reasonable to expect the production of still larger units when economically possible. There is also the hope that silicon power rectifiers will soon be in production and so extend the field of application for these unique devices.

Work on amplifying and power-control devices similar to the thyatron is also very active, and this will no doubt be accelerated with increased knowledge and control of germanium and silicon single-crystal production and processing.

## (12) ACKNOWLEDGMENTS

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## (14) APPENDICES

(14.1) Theory of  $p$ - $n$  Junction Rectifiers

Since many descriptions of semi-conductor physics have been published, it will be sufficient to state as briefly as possible the salient points of the theory and then to examine the governing equations so that the current flow in the practical case can be determined as a function of voltage and the device parameters.

The method employed here will treat the problem as the solution of a set of equations for various boundary conditions.

## (14.1.1) Current Flow in Semi-Conductors and Devices: General Case.

The general equation for current flow in all media is

$$J = \sigma E + \sigma X + \partial \mathcal{Q} / \partial t \quad . \quad . \quad . \quad (4)$$

the first term on the right-hand side representing the flow due to electric force, the second that due to chemical or diffusion force and the third the displacement current. An example in which all three terms might be appreciable is that of a gas-filled valve. Here the flow will be due to the electric field between the plates, to diffusion of the ionized gas particles and to displacement current in the capacitance of the electrodes.

In other cases some terms may be neglected. For metals only  $\sigma E$  is significant, while for insulators only  $\partial \mathcal{Q} / \partial t$  is significant.

## (14.1.2) Semi-Conductor Properties.

In semi-conductors current can flow in two ways: either free electrons can flow through the crystal in a manner similar to the flow in metals, or electrons in the bonds between the atoms can move from bond to bond. However, the bonds are nearly filled with electrons, only a few spaces or holes being present, so that when a field is applied the resultant charge movement, in the bond-to-bond flow, is that due to holes moving in the opposite direction to the electrons. Since a hole is the site of a missing electron it constitutes a positive charge or particle.

Free electrons are those which have been removed from their bonds, leaving holes. Therefore, if no impurities are present the concentration of free electrons and holes must be equal, but if certain impurities are introduced the number of carriers of one type or the other can be made predominant.

## (14.1.3) Conduction in a Semi-Conductor.

Unless very large fields are present in a material, the positive and negative charges must cancel. In an ordinary conductor such as copper the positive charges are the ions forming the structure. Since they are evenly distributed the electrons must also be evenly distributed and no concentration gradient, and resultant diffusion force, can exist.

In a semi-conductor both positive and negative charges can be mobile, and so large fluctuations in density are possible while the average neutrality of charge is maintained. This is the fundamental difference between conduction in semi-conductors and ordinary metals. In semi-conductors both the first and second terms on the right-hand side of eqn. (4) are therefore significant. The displacement term  $\partial\mathcal{D}/\partial t$  is important only in the high-resistance junction layers, and forms the capacitance current.

For semi-conductors eqn. (4) thus becomes

$$J = \sigma E + \sigma X \quad . \quad . \quad . \quad . \quad . \quad (5)$$

Engineers will be familiar with the flow of current due to field as defined by  $\sigma E$ , but may not be familiar with the concept of a chemical force. This is derived as follows.

The flow of particles due to diffusion forces, acting to transfer particles from regions of high density to those of low density, is well known to be proportional to the concentration gradient and is given by

$$\Gamma = -D(dn/dx) \quad . \quad . \quad . \quad . \quad . \quad (6)$$

If these particles were electrons of concentration  $n_e$  and the concentration was such that eqn. (6) was obeyed, the current flow would be

$$J = +eD_e(dn_e/dx) \quad . \quad . \quad . \quad . \quad . \quad (7)$$

But  $D_e$  is related to the mobility of the electron by Einstein's relationship

$$D_e = \mu_e \frac{kT}{e} \quad . \quad . \quad . \quad . \quad . \quad (8)$$

and  $dn_e/dx$  can be written

$$dn_e/dx = n_e d(\log_e n_e + K)/dx \quad . \quad . \quad . \quad . \quad . \quad (9)$$

where  $K$  is a constant.

Substituting eqns. (9) and (8) in eqn. (7) and putting the constants  $kT/e$  inside the differentiation yields

$$J_e = +e\mu_e n_e \frac{d\left(\frac{kT}{e} \log_e n_e + K\right)}{dx} \quad . \quad . \quad . \quad (10)$$

But  $e\mu_e n_e$  is the conductivity of the semi-conductor, and eqn. (10) can be written

$$J_e = +\sigma \frac{d\xi_e}{dx} \quad . \quad . \quad . \quad . \quad . \quad (11)$$

where

$$\xi_e = \frac{kT}{e} \log_e n_e + K \quad . \quad . \quad . \quad . \quad . \quad (12)$$

in which  $\xi_e$  acts as a potential—a chemical potential.

A chemical force can be defined by analogy with electric force by

$$X_e = d\xi_e/dx \quad . \quad . \quad . \quad . \quad . \quad (13)$$

The total current is the sum of the electric and diffusion components and is

$$J = \sigma E + \sigma X \quad . \quad . \quad . \quad . \quad . \quad (14)$$

as in eqn. (5).

In a semi-conductor the electric force will act on both holes and electrons, but the chemical force must be split into two parts: that due to hole-concentration gradient, acting only on holes, and that due to electron-concentration gradient, acting only on electrons.

If  $\psi$  is the electrostatic potential,

$$E = -d\psi/dx \quad . \quad . \quad . \quad . \quad . \quad (15)$$

also

$$X_e = d\xi_e/dx \quad \text{for electrons} \quad . \quad . \quad . \quad . \quad . \quad (16)$$

and

$$X_h = -d\xi_h/dx \quad \text{for holes} \quad . \quad . \quad . \quad . \quad . \quad (17)$$

The signs of these terms are chosen so that the chemical forces are directed in the same sense as that of the current flow rather than the particle flow.

The electron current is

$$J_e = -e\mu_e n_e \left( \frac{d\psi}{dx} - \frac{d\xi_e}{dx} \right) \quad . \quad . \quad . \quad . \quad . \quad (18)$$

The hole current is

$$J_h = -e\mu_h n_h \left( \frac{d\psi}{dx} + \frac{d\xi_h}{dx} \right) \quad . \quad . \quad . \quad . \quad . \quad (19)$$

After giving these results the equations will now be shown as being part of a system of equations which describe current flow in a semi-conductor (Figs. 20 and 21).

The current flow due to fields is given in eqns. (20) and (24), and the change of concentration with time<sup>4</sup> by eqns. (21) and (25). Eqn. (22) is Poisson's equation, which governs the electric potential distribution  $\psi$ . Eqn. (23) shows that the net generation of holes and electrons must be equal when a steady state is reached. Eqns. (26) and (27) define  $\xi_e$  and  $\xi_h$ .

The term  $g'$  in eqns. (21) and (25) accounts for other phenomena (e.g. the generation of light) and is not referred to in the following treatment.

(14.1.4) Current Flow in a *p-n* Junction.

If a device is produced in which one side has an excess of free electrons and the other an excess of holes, the excess of carriers on either side will tend to produce flows over the junction. This will upset the charge distribution and produce a charged layer at the junction.

The distribution after equilibrium is reached can be found by putting  $J_h$ ,  $J_e$ ,  $\partial n_h/\partial t$ ,  $(G_e - R_e)$  and  $(G_h - R_h)$  equal to zero in eqns. (20)–(27). If the zero level of  $\psi$  is made equal to the zero level of  $\xi_e$  at  $x = 0$ , then from eqns. (20), (21), (26) and (27)

$$\psi = \xi_e = -\xi_h \quad . \quad . \quad . \quad . \quad . \quad (28)$$

For equilibrium it can be shown that  $n_e n_h = \bar{n}_i^2$ , and from

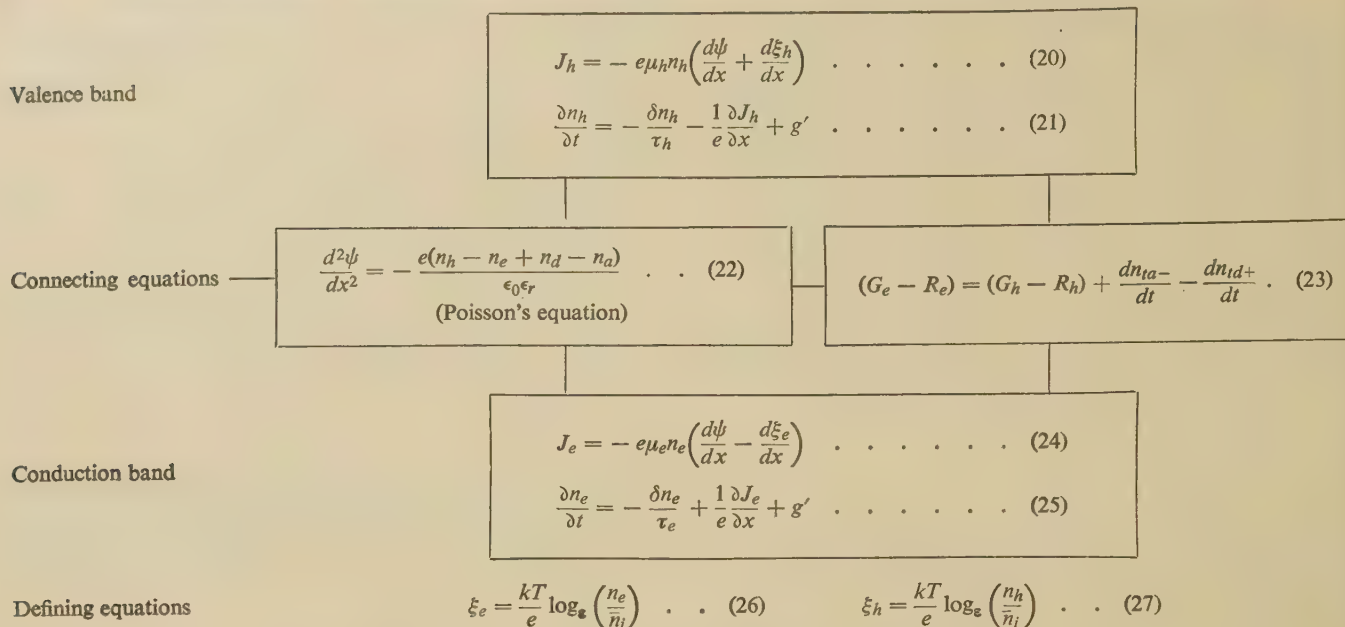


Fig. 20.—System equations for current flow in semi-conductors.

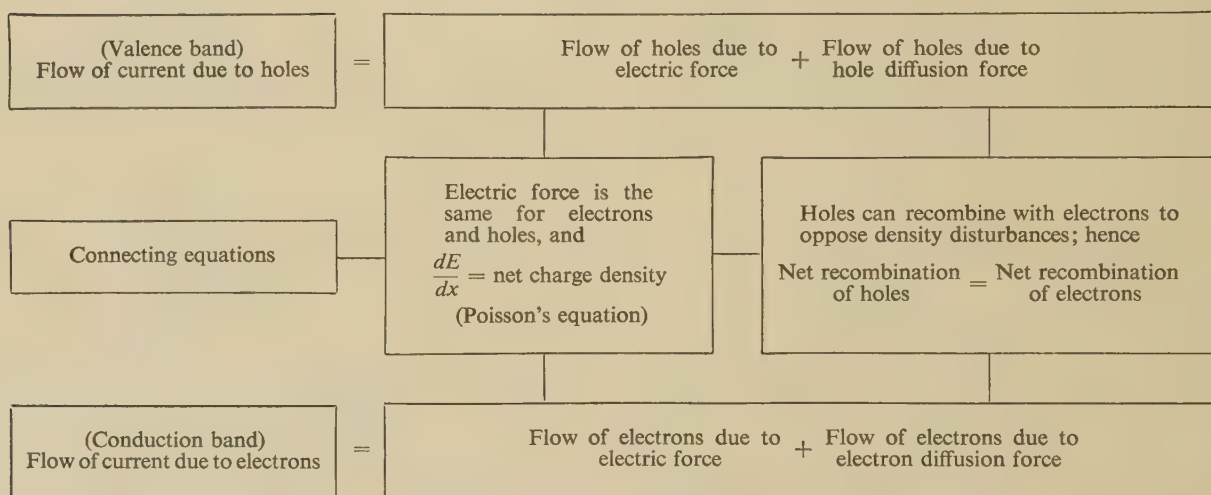


Fig. 21.—Key to equations for current flow in semi-conductors.

eqns. (26), (27), (17) and (22), the final conditions are a solution of

$$\frac{\epsilon_0\epsilon_r}{e} \frac{d^2\psi}{dx^2} = 2\bar{n}_i \sinh \frac{e\psi}{kT} - (n_d - n_a) \quad (29)$$

For a step junction which has a sharp change in impurity type the sinh term can be ignored and an approximation to the charge distribution is given by

$$\frac{\epsilon_0\epsilon_r}{e} \frac{d^2\psi}{dx^2} = -(n_d - n_a) \quad (30)$$

This can be solved for various boundary conditions.

If a voltage is applied between the ends of the device a constant field will be formed across it instantaneously and a current will flow which at every point is proportional to carrier concentration. It is easily seen that if the potential of the *p*-layer is increased with respect to that of the *n*-layer, by applying a positive

voltage to the *p*-layer, there will be a net flow of holes into one side of the junction and a net flow of electrons into the other. This will tend to neutralize the space charge and allow carriers to flow over the junction into the neutral regions. If the potential of the *p*-region is lowered with respect to that of the *n*-layer, the instantaneous net carrier flow will be away from the barrier and will tend to increase the charge layer and so oppose majority carrier flow.

Minority carrier flow will not be prevented, however, and this flow will produce the inverse or saturation current.

#### (14.1.5) Shockley's Equation for Current Flow.

In 1949 Shockley obtained<sup>24</sup> an equation for current flow by making certain assumptions, namely

(a) Injected carrier densities are small compared with existing majority carrier densities.

(b) Outside the space-charge region the electric potential gradient,  $d\psi/dx$ , is negligible compared with the chemical potential gradient.

(c) All injected densities decay by recombination before the end contacts are reached.

(d) The change in  $(\psi - \xi_e)$  across the barrier is negligible.

Since  $d\psi/dx$  is zero in the  $n$ -region eqn. (22) disappears.

In eqns. (20) and (24)  $d\psi/dx$  is zero and the only connection between the two systems in this region is eqn. (23), current flow depending only on the chemical potentials.

The solutions of the remaining equations for a steady state give the current flows over the junction as

$$J_e = \frac{e\sqrt{D_e(n_e)_p}}{\sqrt{\tau_e}}(e^{eV/kT} - 1) \quad (31)$$

$$J_h = \frac{e\sqrt{D_h(n_h)_n}}{\sqrt{\tau_h}}(e^{eV/kT} - 1) \quad (32)$$

The assumptions made show that the equations will be suitable for grown junctions which have long end regions, and for low current densities.

#### 14.1.6 High-Level Injection in Thin Rectifiers.

In the fused-impurity high-power rectifiers described in the paper Shockley's assumptions are not permissible, since very high injected densities are used. Moreover, it is usual to make the thickness of the end region much less than the minority-carrier diffusion length. In this case the carrier distribution will depend upon the conditions at the metallic contact.

The first solution to this problem was obtained by Hall,<sup>25</sup> who assumed that the metallic connection would be a low recombination contact and that volume recombination would be predominant.

For a high injection density he obtained

$$(J_h)_f = \frac{e d \bar{n}_i}{\tau_h} e^{eV/2kT} \quad (33)$$

It will be seen that the equation makes the current increase with increasing wafer thickness. This has not been found for rectifiers made by the authors; in these the current has increased with a decrease of wafer thickness, and the following assumptions have yielded an equation, due to Blundell, which gives results agreeing more closely with practice.

(a) The carrier density is maintained at equilibrium density at the metallic contact (i.e. a high-recombination contact).

(b) Volume recombination is negligible.

(c) Injected densities are large compared with equilibrium densities.

If the fused  $p$ -region is assumed to be of such a low resistivity that Shockley's theory applies in it, the electron current is negligible and a fourth condition is that  $J_e = 0$ .

From Fig. 21 it can be seen that in the  $n$ -region assumption (b) will eliminate eqn. (23).

The remaining equations can be solved as follows:

From the assumption that  $J_e = 0$  and eqn. (24)

$$J_e = -e\mu_e n_e \left( \frac{d\psi}{dx} - \frac{d\xi_e}{dx} \right) = 0 \quad (34)$$

$$\text{Therefore} \quad \frac{d\psi}{dx} = \frac{d\xi_e}{dx} \quad (35)$$

From eqns. (20) and (35)

$$J_h = -e\mu_h n_h \left( \frac{d\xi_e}{dx} + \frac{d\xi_h}{dx} \right) \quad (36)$$

But from eqns. (26) and (27)

$$\frac{d\xi_e}{dx} = \frac{kT}{e} \frac{1}{n_e} \frac{dn_e}{dx} \quad (37)$$

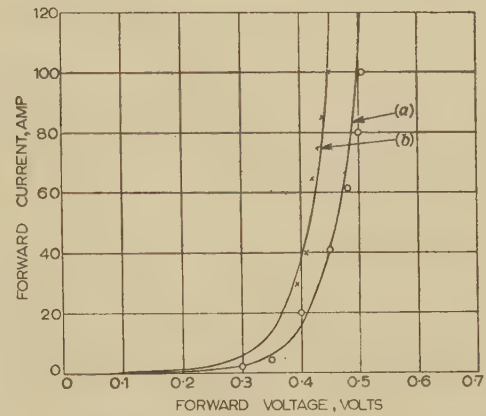


Fig. 22.—Forward characteristics of germanium rectifiers with thin wafers.

Theoretical.  
 x x x } Measured points.  
 o o o }  
 (a) 15 mil wafer.  
 (b) 6 mil wafer.

and

$$\frac{d\xi_h}{dx} = \frac{kT}{e} \frac{1}{n_h} \frac{dn_h}{dx} \quad (38)$$

From eqns. (36)–(38) and noting that  $\mu_h \frac{kT}{e} = D_h$

$$J_h = -eD_h \left( n_h \frac{dn_e}{dx} + \frac{dn_h}{dx} \right) \quad (39)$$

The applied voltage  $V_a$  is given by integrating across the  $n$ -region from the junction ( $x = 0$ ) to the metallic contact ( $x = d$ ).

$$V_a = \int_0^d \frac{J_h}{(\sigma)_p} dx = \int_0^d \frac{J_h}{e\mu_h n_h} dx \quad (40)$$

From eqns. (40) and (36).

$$V_a = - \int_0^d \left( \frac{d\xi_e}{dx} + \frac{d\xi_h}{dx} \right) dx = [\xi_e + \xi_h]_{x=0} - [\xi_e + \xi_h]_{x=d} \quad (41)$$

From eqns. (41), (26) and (27)

$$V_a = \frac{kT}{e} \log_e \frac{(n_e)_0}{(n_e)_d} + \frac{kT}{e} \log_e \frac{(n_h)_0}{(n_h)_d} \quad (42)$$

The subscript 0 indicates values at  $x = 0$ , the junction, and the subscript  $d$  indicate values at  $x = d$ , the metallic contact.

From eqn. (42)

$$(n_e)_0 (n_h)_0 = (n_e)_d (n_h)_d e^{eV/kT} \quad (43)$$

Using assumption (c) and assuming neutrality

$$n_e = n_h \quad (44)$$

From eqns. (39) and (44), for steady-state conditions,

$$J_h = -2eD_h \left( \frac{dn_h}{dx} \right) \quad (45)$$

$J_h$  is constant and so  $dn_h/dx$  is constant and is given by

$$\frac{(n_h)_0 - (n_h)_d}{d} \approx \frac{(n_h)_0}{d}$$

From eqns. (43)–(45)

$$J_h = \frac{-2eD_h \sqrt{[(n_e)_d(n_h)_d]} e^{eV/2kT}}{d} \quad (46)$$

If  $(n_e)_d$  and  $(n_h)_d$  are assumed to be the bulk equilibrium concentrations,  $(\bar{n}_e)_n$  and  $(\bar{n}_h)_n$ , in a region where there is a high concentration of impurities at the base contact, then, since  $(\bar{n}_e)_n(\bar{n}_h)_n = \bar{n}_i^2$

$$J_h = \frac{-2eD_h \bar{n}_i}{d} e^{eV/2kT} \quad (47)$$

Good correlation is obtained between currents determined from this equation and those from practical measurements, as seen in Fig. 22. However, variations in the exponential factor will tend to make exact confirmation of the linear factors rather difficult.

#### (14.1.7) Saturation Current in Thin Rectifiers.

Shockley's equations [(31) and (32)] show that when a reverse voltage is applied to a rectifier the current reaches saturation and remains constant. For holes this current is given by

$$(I_h)_s = -e \sqrt{\frac{D_h(n_h)_n}{\tau_h}} \quad (48)$$

With the necessary assumptions made in the last Section the inverse current would be

$$(I_h)_s = -eD_h \frac{(n_h)_d}{d} \quad (49)$$

Again, if  $(n_h)_d$  is assumed to be the bulk equilibrium value,  $(n_h)_n$ , the current is

$$(I_h)_s = -eD_h \frac{(n_h)_n}{d} \quad (50)$$

Some decrease of  $(I_h)_s$  with thickness has been observed, while its absolute value, calculated by eqn. (50) is of the same order as that measured in practice. For example, the theoretical saturation current of a high-power rectifier with a junction area of 0.64 cm<sup>2</sup> is about 0.6 mA; in practice, a range of 0.4–1.2 mA is obtained.

#### (14.2) Correlation between Reverse Breakdown Voltage and the Properties of the *n*-type Material<sup>26</sup>

*n*-type germanium contains not only donor, but also some other impurities, which act as acceptors or as traps to the electrons and holes. The difference between donors, acceptors and traps may be explained as follows: certain irregularities in the lattice of the crystal, caused by the presence of impurity atoms or defects in the crystal structure, are able to trap free electrons and free holes. When trapping an electron in the conduction band they are called electron traps, and when losing an electron to the valence band, hole traps.

The irregularity, moreover, is called a donor trap if the impurity atom is neutral with a trapped electron and positively charged without the electron. It follows that an acceptor trap is negatively charged with a trapped electron and neutral without it.

The name donor, as distinct from donor trap, is applied if the energy level of a donor type of trap is very near to the conduction band. In this case the trapping efficiency is low under steady-state conditions. In addition, almost all such irregularities lose their electrons to the conduction band if introduced into a regular lattice of pure germanium and thus they produce *n*-type conductivity.

Similarly, the name acceptor, as distinct from acceptor trap, is used if irregularities of an acceptor type have energy levels near the valence band. Such irregularities also have small trapping efficiency under steady-state conditions and they produce *p*-type conductivity by accepting electrons from the valence band, thus generating free holes.

In the basic *n*-type germanium all the donors are positively charged, by definition. Practically all the acceptor traps are negatively charged and all the donor traps are neutral, because the electrons which they have trapped cannot easily be re-excited into the conduction band. From the above reasoning and considerations of neutrality

$$\bar{n}_e - \bar{n}_h = \bar{n}_{d+} + \bar{n}_{td+} - \bar{n}_{ta-} \simeq n_d - n_{ta} \quad (51)$$

In fused *p*–*n* junctions manufactured from *n*-type germanium a space charge develops at the junction between the *p*-region and the *n*-region, accentuated by the presence of a reverse voltage. This voltage draws holes away from the junction on the *p*-side and electrons away from the junction on the *n*-side. Thus an unbalanced charge of negative acceptors remains on the *p*-side and an unbalanced charge of positive donors on the *n*-side. This double layer of opposite charges becomes greater with increasing reverse voltage, as also does the density of field lines which run from positive to negative charges across the junction. It can be shown from theory that, with a higher space-charge density on the *n*-side, a lower reverse voltage is sufficient to produce the same field strength.

In the breakdown region of the reverse characteristic, electrons are removed from the valence band and from donors, acceptors and traps, either by the impact of other electrons or by the field. The first effect is called current multiplication<sup>27</sup> and the second Zener breakdown.<sup>28</sup> Both effects generate free electrons and holes and thus produce the high breakdown currents. In addition, both effects increase with the field strength, which depends on the space-charge density on the *n*-side of the junction.

Since the electrons are removed from both the donor traps and the acceptor traps, this space-charge density is given by

$$en_{eff} \equiv e(n_d + n_{td}) \quad (52)$$

where  $n_{eff}$  denotes the effective density of the charged particles. Therefore the breakdown voltage at a certain breakdown current decreases with increasing values of  $n_{eff}$ . Fig. 23, which is a plot of  $n_{eff}$  against breakdown voltage, demonstrates this.

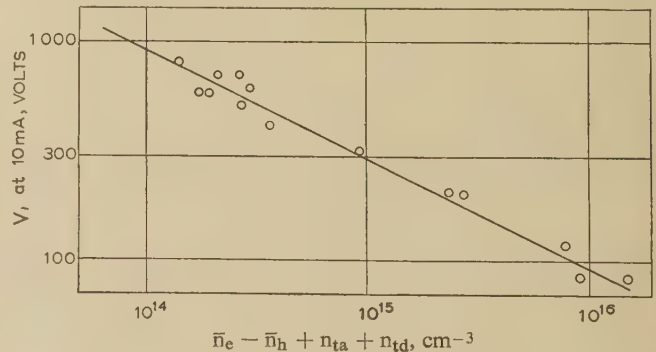


Fig. 23.—Relationship between rectifier breakdown and properties of semi-conductor.

For this curve values of  $n_{eff} \equiv n_d + n_{td}$  in several samples of *n*-type germanium were evaluated from resistivity and lifetime measurements. Batches of power rectifiers (junction area 0.64 cm<sup>2</sup>) were then manufactured from each sample, and the

voltage at which the best rectifier from each batch had a reverse current of 10 mA at room temperature was determined and plotted as breakdown voltage. It can be shown<sup>29</sup> that  $n_{eff}$  cannot be found from resistivity measurements at one temperature, and that, in fact, only  $\bar{n}_e$  and  $\bar{n}_h$  can be evaluated by this measurement. The relationship between  $n_{eff}$  and  $n_e$  and  $n_h$  can be derived from eqns. (51) and (52) and is given by

$$n_{eff} \equiv n_d + n_{td} = \bar{n}_e - \bar{n}_h + n_{ta} + n_{td} \quad (53)$$

from which  $n_{eff}$  can be found, since the concentrations  $n_{ta}$  and  $n_{td}$  can be determined from combined lifetime and resistivity measurements.<sup>30</sup> It can be seen from Fig. 23 and good correlation is obtained between the reverse breakdown voltage and the bulk material measurements.

#### (14.3) Hole-Storage Effect

Meacham and Michaels<sup>20</sup> have described a storage phenomenon in point-contact rectifiers; it is present in  $p$ - $n$  junction rectifiers to a marked degree and is a limitation to high-frequency working in germanium and silicon devices.

The effect is best described by considering the rectifiers referred to in the paper. In these, forward current flows mainly as a current of holes which are injected from the fused  $p$ -region and will eventually recombine in the germanium wafer or at the base contact.

The holes moving across the  $n$ -region of the wafer act as a stored charge, for if an inverse voltage is suddenly applied the stored holes will flow back over the junction before they can recombine. The resistance of the wafer during the forward conduction period is the germanium resistance shunted by the

"modulated" resistance due to the injected holes; for high power units this is about 0.005 ohm. When a sudden inverse voltage is applied the resistance of the rectifier will be unchanged for the first few microseconds of current flow, and large currents may flow during this period, up to two or three times the forward current having been observed. As the stored holes flow back from the  $n$ -region the resistance will rapidly increase and the current will decrease until the normal inverse current is flowing.

The behaviour of a rectifier in a particular circuit depends upon the component values. If the inverse-voltage source has an output resistance larger than the forward resistance of the rectifier, the initial reverse-current flow will be limited by this resistance. The rectifier resistance will rise rapidly, and at some time, perhaps 20 microsec after the voltage reversal, the rectifier resistance will rise rapidly above the source resistance and cut off the current. This gives a typically square current pulse and has been described by Pell<sup>31</sup> and others.

A case of particular interest occurs if the source impedance is inductive. When the change-over to inverse voltage occurs the current will decrease and become negative, and while the rectifier resistance is low the current is limited by the inductance and follows a typical exponential curve. As previously explained, the rectifier resistance will now start to rise, the current suddenly decreasing, and this rate of change of current can be much greater than the initial rate of change on reversal. Thus a large voltage spike is induced across the inductance and may destroy the rectifier, as discussed in Section 10.2.2. For example, a rectifier working at 70 volts peak inverse voltage may be subjected to spikes of up to 200 volts.

### DISCUSSION BEFORE THE INSTITUTION, 10TH NOVEMBER, 1955

**Dr. W. G. Thompson:** The extraction of germanium from flue dust is a story with which I should like to associate the name of Mr. R. C. Chirnside; in devising successful methods of extraction, particularly in connection with the removal of arsenic mentioned in Section 3.1, he provided the vital link between Goldschmidt's work at Göttingen and the commercial production carried out by Powell, Lever and Walpole.

It is true to say that the physics of to-day is the electrical engineering of to-morrow, and germanium and silicon rectifiers are excellent examples of this. They are also examples of an equally important principle that it is no use attempting a development unless a real margin of advantage is present over equipment already available: there must be positive economic gains without sacrifice of reliability. The economic gain in the present instance arises from the low forward voltage drop from which the correspondingly reduced losses give lower operating costs and smaller physical size. In this respect germanium and silicon represent a considerable advance over the selenium and copper-oxide rectifiers, which work on the principle of the potential barrier between metal and an oxide or sulphur film. These rectifiers have current densities of 0.25 and 0.1 amp/cm<sup>2</sup> respectively, compared with germanium's 100 amp/cm<sup>2</sup> for the same forward voltage drop of 0.6 volt. The increase in current density by a factor of 4000 gives germanium a margin of advantage which permits adjustments in other directions, including the maintenance of the proper factors of safety.

The economics of the new rectifiers are bound up with the forward voltage drop and the reverse voltage which each element can withstand. The higher the withstand voltage the fewer the discs in series for a given working voltage and the lower the total losses. In Fig. 16 the authors have made it evident that the germanium rectifier maintains this advantage over the mercury-arc rectifier even at 600 volts, and it is comparable with

the mechanical rectifier without its attendant disadvantages. Special circumstances led to the development of the mechanical rectifier in this country, and it would be interesting to have the authors' opinion on its future in the light of the success with germanium.

On the basis of a peak reverse voltage of 100 volts, the losses of a germanium rectifier should equal those of a mercury-arc rectifier at about 1500 volts d.c.; but it should be borne in mind that the valve action can be momentarily lost in a mercury-arc rectifier and the rectifier can return to service immediately after a backfire, whereas permanent damage would result if reverse fault current should flow in a germanium rectifier. I should like the authors to comment on the transient reverse-voltage behaviour of the germanium rectifier, because, from their remarks about Fig. 15, surges of microsecond duration appear to be important. What remains to be seen, therefore, is whether for practical service conditions the presence of surge phenomena may make it desirable in the interests of reliability for the discs to be rated much more conservatively. Experience alone will show whether this will happen, but in the event of its being necessary to revert to, say, 50 volts reverse per disc, the efficiency advantage of the germanium rectifier over the mercury-arc rectifier would disappear at 750 volts d.c.

The method of rating indicated in Section 3 is slightly misleading, because there is a temperature term implicit; instead I would prefer the equation to be presented in the form

$$P = \frac{\theta_i - \theta_a}{S}$$

thus splitting  $P$  into  $P_f$  and  $P_r$ ,  $P_r$  being expressed as

$$P_f + P_r = \frac{\theta_i - \theta_a}{S}$$

and transferred to the right-hand side of the equation.  $P_f$ , forward loss, is of interest and is given by

$$P_f = \frac{\theta_i - \theta_a}{S} - k\theta_i$$

This should make it clear that  $P_f$  is zero long before  $P_r$ .

Turning to practical details, the importance of thermal capacity in limiting temperature rise under fault conditions should be borne in mind. While the form of construction shown in Fig. 9 may be satisfactory for ordinary working, it may require modification for arduous duty. We have found no objection to mechanical forces, provided that they give rise only to direct compressive stresses. The authors give a good account of the cooling problem, but I would prefer the term "latent-heat cooling" to "vapour cooling," particularly as it has been established that so long as there is sufficient condensation to cover the whole surface there is little advantage in filling the cooling space with liquid, except in providing thermal capacity.

Fig. 15 represents a circuit phenomenon which I encountered many years ago in connection with gas-blast rectifiers, which, in that case, was due to the excessively rapid quenching of the arc in the final stages of conduction; it can also be present in heavily-deionized high-voltage mercury-arc rectifiers, and it is interesting to note that the authors recommend the time-honoured remedy of by-pass capacitors.

**Mr. T. E. Houghton:** Until comparatively recently the choice of converting plant for heavy-current duty, i.e. for d.c. outputs exceeding 50 kA at medium voltages, was limited to motor-convertors for normal use or motor-generators where a wide range of voltage and close control of current were required. Both types have given excellent service; in particular, the motor-convertor has a relatively high efficiency, it is compact and its maintenance costs are low. At medium voltages the mercury-arc rectifier has a poor efficiency, it occupies much space and is expensive.

no less serious than the backfire of a contact rectifier. However, this defect should not be difficult to correct.

The authors refer to the voltage oscillations which follow the sharp spike in the voltage waveform in the current circuit, and Fig. A(i)—an oscillogram of the direct voltage of the 1 MW

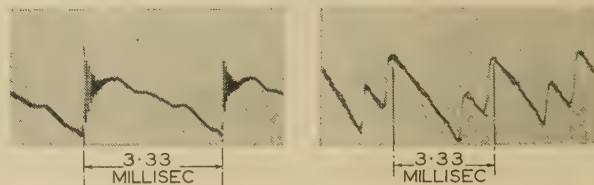


Fig. A.—Comparison of output-voltage waveforms.

- (i) 1 MW germanium rectifier: 251 volts (mean) at 3.9 kA.  
(ii) 2.4 MW contact rectifier: 240 volts (mean) at 10 kA.

rectifier—shows that there is a 15 kc/s oscillation at the commencement of each rectification. Will this affect radio reception and telecommunication generally? So far, we have had no complaints, but with a large installation trouble from this cause might be serious. There are no such oscillations in the direct voltage from the contact rectifier, as will be seen from Fig. A(ii).

In their comparison of the efficiencies of various types of convertor for an output of 15 kA (Fig. 16) the authors are a little optimistic about the germanium rectifier. The overall efficiency of the 1 MW equipment (from loss measurements) is 96.5%, i.e. 0.8% lower than that of a comparable contact rectifier; whether this is sufficient compensation for possible trouble due to backfires has yet to be determined, but it is not a negligible item for a plant operating 8760 hours a year.

Table A gives a detailed comparison of convertors, based on actual installations or on present-day designs.

If the germanium rectifier fulfils its early promise and has a

Table A

CONVERTORS GIVING 80 kA AT 270 VOLTS

Type of rectifier	Efficiency	Power factor	Fault contribution	Crane capacity	Area required*	Volume of buildings*	Relative cost
	%		MVA	tons	ft <sup>2</sup> /kW	ft <sup>3</sup> /kW	
Motor-convertors ..	92.8	1.0	75	50	0.6	26	100
Mercury-arc rectifiers ..	90.2	0.94	0	5	1.0	32	139
Contact rectifiers ..	97.3	0.87–0.90	0	12	0.4	11	79
Germanium rectifiers ..	96.5	0.88–0.92	0	0	0.4	7.5	74

\* Allowing for all switchgear and auxiliaries.

Since the war, however, the contact rectifier has been developed to a higher degree of perfection; its efficiency is high, it is very compact, it requires a relatively small building, its availability is about 98% and its maintenance cost is extremely low. True, it may backfire and trip out on a system disturbance, but this is not so serious as might be supposed, and on private systems, where system disturbances are infrequent, it is not a real trouble.

The germanium rectifier undoubtedly shows great promise, for although it may have a slightly lower efficiency than the contact rectifier, it has the great advantage that there are no moving parts; it is very compact and it should be cheap. On the other hand, it is not yet known whether the germanium elements will age, so that its life is as yet uncertain. Moreover, the authors' 1 MW equipment depends for continuous service on the running of two small fans, and if either fails the rectifier trips out, which is

life of at least 100 000 hours, its users will be satisfied. This life is not excessive, for I have a large motor-convertor which recently gave 87 000 hours of service without attention to its commutator.

**Dr. E. Eastwood:** The studies of the germanium power rectifier conducted by my colleagues and me have resulted in a semi-conductor device differing in some particulars from the one presented by the authors.

We used the horizontal-boat technique for the purification of the material and for growing the final doped single crystals. A crystal grown from the melt by the withdrawal technique possesses a resistivity profile which varies along the length of the crystal, whereas the horizontal-boat technique can produce a level profile. The economics of the production process is thereby improved, because it is now possible to cut slices having identical electrical characteristics from the whole length of such

crystal. Oblique sections having the required (1 1 1) orientation can be taken without additional waste, thereby giving wafers of cross-section considerably in excess of 300 mm<sup>2</sup>, with corresponding increase in the rectifier rating.

We consider that the low-junction-temperature requirement can best be met by using liquid cooling; our design is such that the cooling liquid is allowed to impinge directly upon both the anode and the cathode sides of the wafer, the flow being turbulent and the cooling therefore very efficient. The ratio of heat removal through anode and cathode respectively is of the order of 40 : 60. The temperature of the junction is correspondingly reduced and the safety factor improved. Junctions of the *p-n* type have been produced by the indium-alloying process, but we have found it advantageous to provide for uniform wetting of the germanium by application of the indium in the form of a prepared anode element. Pulse etching has been applied to procure more stable junction characteristics. The mount has been designed to minimize different thermal expansion effects and allow hermetic encapsulation.

The current/voltage characteristic shows the following points:

(a) There is a forward voltage drop of less than 0.5 volt at 250 amp, which is the loading for a single rectifier unit; this is in accordance with the increased junction area with which we are working.

(b) There is no sign of turn-over, even at the reversed potential of 250 volts; turn-over voltages up to 450 volts have been achieved. We consider that the working peak inverse voltage should be taken as one-half to one-third of the turn-over voltage, depending on the nature of the circuit. This is an insurance against induction effects due to switching and hole storage.

The unit is conservatively rated, since for a load of 250 amp (mean) the temperature rise is only 37°, corresponding to a junction temperature of 50°C. We consider that a junction temperature of 70°C is safe, but have successfully operated test units at 100°C.

**Mr. J. A. Broughall:** British Railways are very interested in the application of rectifiers and have been watching this development closely. We seem to be offered two new tools, the silicon rectifier and the germanium rectifier, but I gather that the mercury-arc rectifier is not yet obsolete. The traction engineer is interested in rectifiers suitable for reverse voltages of 500–900 volts, for use on locomotives and multiple-unit trains with high alternating voltages on the overhead line.

We are now assembling a 750 kW germanium rectifier for trial on a multiple-unit train for traction duty, and hope soon to be able to report the successful trial of the first germanium-rectifier train in the world.

On an overhead traction line the voltage surges are not quite as serious as might be expected. The only case of which I have exact knowledge—and that was on a d.c. system, which may be more prone to surges than an a.c. system—suggests that this is so. The germanium rectifier is clearly sensitive to surges, but I presume that they can be suppressed in the transformer.

We think we shall not have more trouble with harmonics with germanium than with other rectifiers, but I should like the authors' assurance on this point.

Finally, I hope that the authors will continue with the concept of using dirty railway air to cool the rectifier. If they need an intervening fluid to give better heat transfer, I hope that they will use ordinary mineral oil rather than water. They should not spoil a good tool by surrounding it with water, which can freeze, or by some complicated fluid which, when there is an accident, may generate poisonous gas.

**Major L. H. Peter:** In Section 6.2 the authors say that the germanium will precipitate out of solution to form a rich surface layer of heavily impregnated crystals, which seems correct, because they are freezing a solution of germanium to which indium has been added. But what is the transition from the

polycrystalline material to the monocrystalline material? Is any portion of the rectifying action occurring at the junction of the indium and the polycrystalline material? If so, why is the monocrystalline material wanted initially?

The British Standards Institution has set up a committee on the standardization of some semi-conductor devices, and it is clear that this should be extended to cover all the monocrystalline devices we are now discussing. If we could secure agreement on methods of expression and testing, we should be in a better position when this matter comes up for discussion in the International Electrotechnical Commission, where it is already scheduled.

**Dr. J. C. Read:** Whilst the inception of the semi-conductor rectifier resulted from the brilliant American research work which has been mentioned, its engineering application has reached an advanced position in this country; the progress made may be judged from the fact that germanium rectifiers amounting to over 25 MW are already operating or in course of manufacture here.

The opinion has recently been expressed abroad that in the low-voltage heavy-current electrolytic field the mechanical contact rectifier and the semi-conductor rectifier will long continue to compete with one another, on account of the efficiency advantage of 1/4–1/2% possessed by the former. I have two comments to make on this. First, whereas the efficiency of the mechanical rectifier has about reached its limit, it is probable that the efficiency of the germanium rectifier will be further increased in the near future. Secondly, this efficiency comparison is based on the summation of estimated losses, and in these the eddy-current and skin effects in the large a.c. conductors between the transformer and the rectifier are not fully allowed for. These extraneous losses can be made much lower (by subdividing the a.c. circuits) with the semi-conductor rectifier than is possible with the mechanical rectifier, so even at present there is, I think, comparatively little difference in real efficiency between them. To this point must be added the facts that the power factor of the semi-conductor rectifier is higher than that of the mechanical rectifier by at least 5% and often as much as 15–20%; that it can be housed in a cheaper building and requires no crane; and that, unlike the mechanical rectifier, it is not sensitive to distortion or transient drops of the supply voltage.

For high-voltage electrolytic processes, on the other hand, and for stationary traction substations, the mercury-arc rectifier is at present still supreme. However, for mounting in a locomotive or multiple-unit train for mains-frequency railway service, the semi-conductor rectifier appears to offer a number of important advantages. It is lighter, smaller and can more easily be fitted into awkward spaces than the mercury-arc rectifier; its protection is simpler; and it is almost unaffected by mechanical shocks and vibration. Compared with some mercury-arc valves it has the merit of eliminating liquid cooling, which is a source of trouble on a railway vehicle. Unlike the ignitron, it is unaffected by a heavy drop in the supply voltage, and is unharmed in the event of a loss of a.c. supply and therefore need not have any of its cooling apparatus supplied from the battery. Furthermore, its temperature control is much simpler, since very low temperatures are not detrimental to it, and in consequence a train can stand in a siding through a cold night and pull straight away in the morning without requiring initial heating.

The 750 kW germanium rectifier recently built for trial on a 50 c/s section of British Railways is designed for a working peak inverse voltage of about 5 kV, since it must operate with an existing transformer originally built for a mercury-arc rectifier for that voltage. Future equipments of this type, designed as a whole from the outset for semi-conductor rectifiers, will probably have a working peak inverse voltage of about 1.5 kV, and for

multiple-unit stock will no doubt have a modified form of rectifier enclosure suitable for under-coach mounting.

The paper states that  $p$ - $n$  junction rectifiers cannot be used for inversion, and I would like to query this. By connecting suitably premagnetized commutating reactors in series with the rectifier it would be possible to delay the commencement of commutation by any desired amount. Inverted operation, as with a grid-controlled mercury-arc rectifier, could thus be achieved. Naturally, in most cases this would not be an economic form of inverter to employ; but it is a theoretical possibility, which may have usefulness in some cases. It is an interesting and little-appreciated consequence of this that the feature which is indispensable in a device to be used for inversion is not that it must possess a control grid, but that it must be able to rectify.

**Mr. L. J. C. Connell:** The main directions for further advance are, perhaps, in achieving higher operating temperatures (for which silicon looks so attractive) and higher peak inverse voltages. There is still much to be learned about the basis for fixing these ratings. For example, it is not altogether clear why the high-power rectifier shown in Fig. 8(c) (which I assume to be the type having a junction area of  $64\text{ mm}^2$ ) is given a peak inverse

power dissipation, could not the peak inverse voltage be increased substantially? Or is the lower figure chosen because of some other rating factor of the rectifier or because it is necessary to safeguard against high transient voltages developed through some external circuit conditions?

Fig. B shows a heavy-current germanium rectifier which has been developed by a group with which I am associated. This rectifier is fundamentally similar to those described in the paper, but it has a larger junction area ( $180\text{ mm}^2$ ). Although there is nothing in the construction that demands liquid rather than air cooling, the junction in this particular sample is cooled by water circulating through the two end electrodes. A group of three of these rectifiers, each no larger than a coffee cup, has delivered  $1\text{ kA d.c.}$  in a 3-phase half-wave circuit with a peak inverse voltage of 70 volts per rectifier.

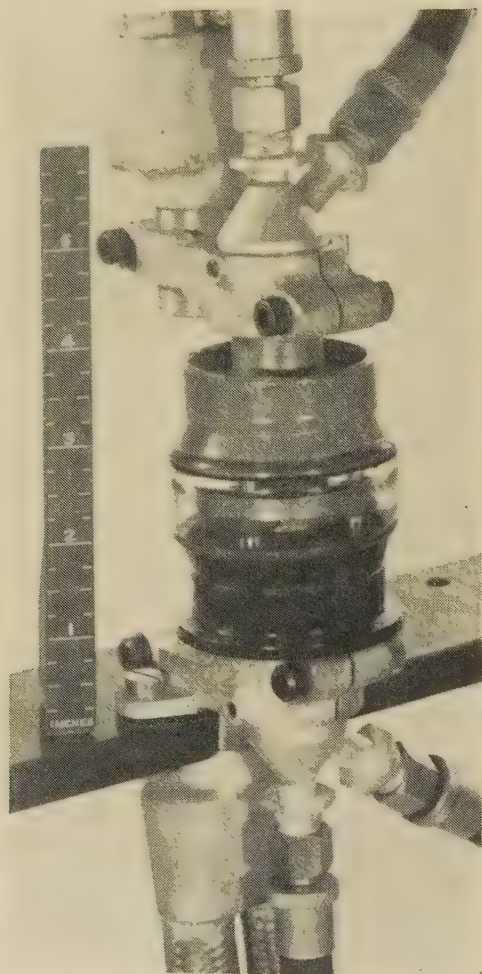


Fig. B.—Liquid-cooled germanium power rectifier, with end leads and cooling pipes.

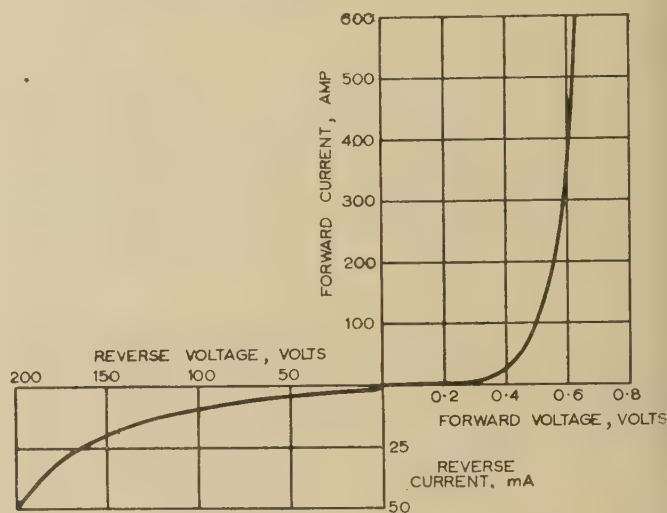


Fig. C.—Characteristics of germanium power rectifier with water-cooled junction.

Fig. C shows the forward and reverse characteristics. It will be seen that even at these higher current densities it is still possible to maintain the low forward voltage drops which are characteristic of the rectifiers described by the authors.

**Mr. A. Langridge:** In a description of the rectification action one generally assumes that the lattice is continuous, but in fact there is a discontinuity of the valence bonds of germanium atoms where the lattice comes to the surface, i.e. there are some atoms with electrons dangling in free space, as it were.

It is possible that, in the very pure germanium crystal, if there were no covering on these atoms, there would be a low-energy path for conduction carriers to flow, so that the reverse current in the rectifier would be rather high. Carasso has shown that if there are impurities of different sorts on the surface the reverse saturation can be affected in many ways. In some cases the reverse current is quite small. He describes this as an inhibition effect.

However, if other impurities are placed on the surface of the germanium they will cause the reverse leakage to be very high. If one regards the rectifier as being absolutely free from surface impurities, it is likely that the loose bonds available will cause this lowering of energy. Therefore, the basic rectifier may be really a worse device than one which is covered with the proper impurity, or rather an impurity which will allow the inhibition effect to occur and to minimize the reverse current.

Do the authors think that their rectifier requires some sort of

voltage rating of no more than 100 volts. The curve shows that at  $70^\circ\text{C}$  and with 300 volts maintained continuously, the reverse dissipation is only about 6 watts. By making a slight reduction in the forward current rating to avoid having an excessive total

capacitor layer on the surface to keep the saturation current down, and, if so, is the lifetime of the device dependent upon such a layer?

**Mr. P. Bingley:** In Section 10.1.2 the authors draw attention to the fact that, with these rectifiers, transformer resistance makes a significant contribution to output-voltage regulation, whereas with other types of rectifier the effect is largely swamped by forward losses; but I cannot see that this constitutes a special argument for adequate protection of the supply system.

The second paragraph mentions surge ratings in relation to peak current demand. What ratings are attributable to the last five types listed in Table 1? From Section 9.2.5 one might expect such ratings to be relatively low.

### THE AUTHORS' REPLY TO THE ABOVE DISCUSSION

**Messrs. T. H. Kinman, G. H. Carrick, R. G. Hibberd and A. J. Blundell (in reply):** The efficiencies of germanium, contact and mercury-arc rectifiers have been compared by several speakers. The efficiency of 96.5% quoted by Mr. Houghton includes long connecting cables and a previously existing mains supply transformer; 97% could be obtained on a completely new equipment. Dr. Read emphasizes that, while the efficiency of contact rectifiers is measured by the summation of a number of losses, the loss of a germanium rectifier is obtained by a single measurement which is probably more reliable.

Dr. Thompson's alternative method of expressing the ratings is appreciated; that given in Section 3 was developed chiefly for its initial determination, rather than for its complete expression.

Dr. Thompson also suggests that in the interests of reliability it may be desirable to operate germanium cells at a peak inverse voltage lower than 100 volts. However, we feel that it will soon be possible to exceed this value. Considering the newness of semi-conductor rectifiers and the possible improvements in working voltage and forward voltage drop, we think that rapid advances will occur, that not many more contact rectifiers will be built in this country, and that the mercury-arc rectifier will shrink more and more to the high voltage end of its domain.

A  $p$ - $n$  junction can fail, owing to avalanche breakdown, under a high reverse voltage lasting only a few microseconds. The breakdown voltage, however, is lower just after the passage of forward current, because holes and electrons remain in the barrier region. The large margin of no-load test voltage above working voltage, mentioned by Mr. Connell, allows for this effect, ensures reliability and covers any voltage surges. The capacitors used to accept reverse current at the end of the conducting period also serve to diminish any surges coming from the a.c. or d.c. system.

The two voltage waveforms shown in Fig. A do not include the voltage zero, but the r.m.s. value of the 15 kc/s oscillation is less than 1% of the direct voltage. It is clear that the germanium-rectifier voltage has much smaller 600, 900 and 1200 c/s components than the contact rectifier, and is thus less capable of causing telephone interference. The 15 kc/s commutation oscillation is similar to that occurring in mercury-arc rectifiers, but owing to the capacitors the amplitude is reduced and the frequency lowered, so that the radio-interference-producing voltages which occasionally arise with mercury-arc rectifiers are practically eliminated. This oscillation is almost blocked by the main transformer, so that the only similar oscillation appearing on the primary side is one of 0.5% at 10 kc/s, which is

In the Sections dealing with circuit connections I notice that current-balancing reactors are dismissed as cumbersome, and that reduction of average current is advocated to secure current balance. Examination of Fig. 19, however, indicates that there are 75 pairs of groups of rectifiers protected by twice as many fuses. Presumably the totals increase roughly proportionately for the 18 MW equipment mentioned. For every parallel path in which current balance is required, a small increment of de-rating is presumably necessary, and it would seem that on larger equipments a stage could be reached where current-dividing reactors, though bulky, would be warranted in order to avoid excessive de-rating. Is this assumption correct, and, if so, is this stage reached within the size of equipments at present contemplated?

difficult to see on an oscillogram and could cause no interference with communication.

The comparisons in Table A are most interesting, and our only comment is that the newcomer, the germanium (or silicon) rectifier, will improve on the figures quoted in every column except perhaps the fourth and fifth.

The life of germanium-rectifier cells is mentioned in the paper, and no experience so far leads us to think that 100 000 hours is impossible with freedom from initial contamination and satisfactory hermetic enclosure.

Among many possible applications, a.c. traction is—for reasons given by Dr. Read—one of the most favourable. The experimental motor-coach announced by Mr. Broughall has now had entirely satisfactory trials, including the short-circuiting of the field of one of a pair of motors. We are confident that satisfactory cooling for this and most applications can be obtained with ordinary air.

Mr. Bingley's question about surge rating can be answered by saying that the cells can withstand about 10 times full-load current when protected by ordinary circuit-breakers. The derating of cells when there is a large number of parallel paths is no greater than on a smaller equipment.

Dr. Eastwood states that he has achieved a turnover voltage of 450 volts. We can well believe this, since the higher grades of our production types are tested at 600 volts and these will safely operate at peak inverse voltages of 280 volts.

In reply to Major Peter, we believe that when the germanium recrystallizes into the wafer it grows initially as a single crystal with the orientation of its planes following those of the wafer. However, because of the high indium content and rapid cooling, this growth soon changes into polycrystalline form. The junction between the monocrystal line and polycrystal line regions may be considered as simulating a metal contact, although it must be admitted that the physical and electrical characteristics of this region have not yet fully been investigated.

We agree with Major Peter's suggestion that the B.S.I. standardization committee should include germanium and silicon power rectifiers in its specifications as soon as sufficient data are available.

The interesting phenomenon of surface conduction mentioned by Mr. Landridge is of the greatest importance and demands much closer attention. We believe the germanium wafer has always a surface layer of foreign atoms produced by the particular surface treatment used. In our experience this surface layer appears to be extremely stable, which is not surprising, since in silicon, for example, it could be silica—a very stable substance.

# TIMING THE OPERATION OF CONTROL SYSTEMS ASSOCIATED WITH ROTATING EQUIPMENT

By C. CUTHBERT, B.Sc., and D. A. PICKEN, Member.

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## SUMMARY

The problem of the exact time of operation of a contactor or braking device in a sequence is a common one, and by the application of the technique outlined in the paper, with simple apparatus times can be measured to better than one-thousandth of a second. The application of this technique to a variety of methods of stopping two-roll mills, as well as illustrating factors such as the time-constants of machinery, discloses some new mechanical problems about which very little is known, e.g. brake glide.

## (1) INTRODUCTION

The technique which is the subject of the paper was developed to measure the time of operation of braking systems associated with two-roll mills used in the rubber industry, and the application of the device to this purpose is dealt with in the paper. However, the method is applicable to a wide variety of purposes, such as the timing of automatic-starter equipments, measuring acceleration curves for motors, timing the operation of protective equipments, etc., and in the paper the application to the rubber-industry problem is merely used as an indication of what can be done and the technique of achieving it.

In the rubber industry use is made of mills to break down crude rubber and to mix this broken-down rubber with chemicals for processing the rubber. These machines consist of horizontal chilled cast-iron rolls, sometimes as much as 8 ft long  $\times$  2 ft 6 in diameter driven by motors frequently of the order of 500 h.p. It is necessary for the operators to work on the rubber whilst it is being rolled, and there were a number of accidents each year owing to people becoming trapped between the rolls and thus suffering grievous injuries. This problem was frequently referred to in annual reports of H.M. Chief Inspector of Factories, and as a result of these reports a committee of the rubber industry was set up in 1949 which produced a report issued in 1952,\* in which it analysed the accidents and suggested mechanical modifications to the layout of the machines and to operating techniques. The basic recommendations were to lower the level of the man relative to the roll and to put a sensitive guard at about the level of his armpit, so that if he tended to be pulled in towards the nip of the rolls he would operate this sensitive guard which would actuate a braking device.

## (2) BRAKING DEVICES

Braking devices were usually of an electrical nature, and they operated in five ways as follows:

(a) Electro-mechanical braking, in which the brake shoes are held off either by a latch which is released by a solenoid magnet, or the brake is held off directly by a solenoid. In this latter case, the actuation of a sensitive guard operates the solenoid to allow the brake shoes to come into contact with the drum.

(b) Dynamic braking on d.c. machines, which consists in a dis-

connection of the supply from the motor and the substitution of a resistance, the dynamo effect providing the braking force.

(c) Dynamic braking on a.c. synchronous machines. The motor is disconnected from the supply, a resistance is substituted and the field is forced by application of an external direct current about  $1\frac{1}{2}$  times the normal value.

(d) Dynamic braking by d.c. injection. With asynchronous machines, direct current is applied to the motor after its disconnection from the a.c. supply.

(e) "Plugging," which consists in reversing two phases on 3-phase motors.

The typical times which are available for stopping the drives are of the order of 1 sec, so that whatever device is utilized it must operate quickly and also be capable of absorbing large kinetic energies.

As an example of the method of operation let us consider the simplest device, i.e. the electro-mechanical brake. The effect of the operation of the sensitive trip bar is to operate a switch which normally opens a circuit supplying a contactor, so that the decay of the magnetic field allows auxiliary contacts to move which actuate the operation of the brakes by breaking the contact from the solenoid and tripping the supply to the motor. Delay times are experienced in the time of collapse of the field of the first contactor. Delay in these can be considerable, possibly of the order of  $\frac{1}{4}$  sec. After this delay the second devices are operated, namely the tripping of the circuit-breaker supplying the motor and the collapse of the field of the solenoid-operated brake. Again, considerable time delay occurs which also may be of about  $\frac{1}{4}$  sec or more. Thus before the brake shoes can come into contact with the brake drum, about  $\frac{1}{2}$  sec can elapse, which may leave only about  $\frac{1}{2}$  sec to absorb the energy, even if the brake shoe operates immediately.

## (3) METHOD OF MEASUREMENT

The measurement of the exact time at which each of the events in the sequence takes place could be made by several methods. Similar problems have been solved by using photographic records of oscillograph traces. After consideration of a number of alternative methods, the use of an electrosensitive paper was adopted. If a piece of paper of this type is attached to the surface of one roll and a stylus is connected to the normal a.c. supply through a resistor and potentiometer, so that the voltage applied to the stylus is of the order of 170 volts (r.m.s.), then as the roll rotates a series of dashes will be drawn on the paper whenever the voltage applied to the stylus is above about 160 volts (instantaneous). Thus 100 of these dashes will be drawn every second, and by measuring the distance between corresponding points of each, the exact speed of the roll can be determined.

Further styli are applied to the paper to indicate the actuation of the various devices. These may take their supply either directly from the switches controlled by the sensitive bar and the supply to the solenoid, or through micro-switches which are actuated by the movements of these former switches, so that if the apparatus is set in action and the trip bar operated, the cessation or commencement of the trace connected to the trip-bar

\* "Safe Working on Horizontal Two-Roll Mills," Appendices III and IV, National Joint Industrial Council for Rubber Manufacturing Industry, Manchester, 1952.

Mr. Cuthbert is with the Leyland and Birmingham Rubber Co., Ltd.  
Mr. Picken is in the Factory Department, Ministry of Labour and National Service.

switch will indicate the time of actuation of the stopping device. The time of opening of the auxiliary relay will be indicated by the cessation of the supply to the motor and the solenoid, and the time of the operation of the solenoid-operated brake can be indicated by the opening of a micro-switch in contact with the brake shoe.

The effect of the braking thereafter can be measured by analysis of the traces, which, if the timing voltage is carefully adjusted, can by the use of a travelling microscope be interpreted until very shortly before the drive comes to a standstill. The distance of stopping is measured directly by the length of the timing trace from where the tripping switch operates to the point at which the trace ends. The timing of this can be determined to about 5 or 10% of the initial speed, and by plotting a curve and extrapolating, the time to zero speed can be readily determined.

If greater accuracy of timing is required, the timing circuits can be fed by means of higher-frequency supply, e.g. 500c/s, in which case the accuracy of timing can be better than one-thousandth of a second, since it is found that with the 50c/s supply, timing to an accuracy of 5 millisecl is quite practicable.

The results of tests carried out by this method on electro-mechanical brakes, regenerative braking on both d.c. and a.c. machines, and on "plugging," i.e. phase reversal on a.c. machines, are given in Sections 4 and 4.1.

### (3.1) Experimental Method

The apparatus shown in Fig. 1 consisted of an adjustable support for an aluminium tube which carried at one end six styli constructed from No. 26 s.w.g. steel wire. The tube was

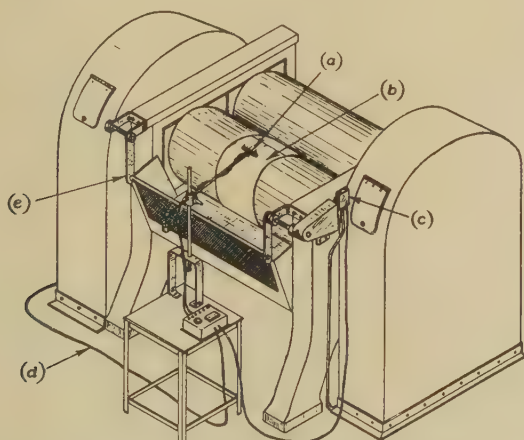


Fig. 1.—Apparatus in position for a test.

- (a) Six steel styli.
- (b) Teledeltos paper.
- (c) Emergency switch.
- (d) Connections to relays and micro-switches.
- (e) Safety bar.

adjustable vertically and horizontally and was pivoted about the support to enable the styli to move in a vertical arc. A sliding weight was fitted to the tube to enable the load on the styli to be adjusted to the required value. Flexible leads connected the six styli to a switch box which contained a 10 000 ohm resistor and a switch for each of five leads. The sixth lead was connected via a 10 000 ohm resistor and a potentiometric circuit to enable the voltage to be varied on one stylus. This was done to enable the voltage to be adjusted to give the best possible trace for measuring purposes on one stylus.

A suitable length of Teledeltos recording paper was fastened to the surface of the mill roll by means of adhesive tape, and a length of smooth paper was fixed to overlap the Teledeltos paper

in such a way that, when the roll revolved, the styli would ride along the roll on to the smooth paper and then on to the Teledeltos paper. The voltage-adjusted stylus was connected to a 230-volt 50c/s a.c. supply, and the other styli as required to parts of the electrical circuit operating the brake or to micro-switches which were operated mechanically by the movement of brake shoes or contactors. In some cases the arrangement of the circuit caused the stylus to be live before the operation of the device to which it was connected, and in other cases the stylus became energized only when the particular contactor or switch was operated. Thus the dotted trace would stop or start, as the case might be, when the particular device to which the stylus was connected was operated.

The individual styli were adjusted by gentle bending until all the points lay on a straight line. The instrument was placed on a suitable support near the mill roll with the styli in contact with the roll surface. The sliding weight was moved until the load on the styli was about 80g when measured by a spring balance hooked over the strip carrying the styli. The switches were set in the off position, and the mill was set in motion. After final adjustment to ensure that all the styli were in the correct position, the stylus switches were operated when the styli were on the smooth paper, and the emergency switch was pressed when the styli had started marking the Teledeltos paper. The test was repeated three times.

All the measurements were made on empty mills, since it is considered that the rubber itself assists brake action, and hence the empty mill is the one having the longest braking distance.

### (3.2) Analysis of Trace

A typical trace is shown in Fig. 2. The trace requiring analysis commenced as soon as the emergency switch was

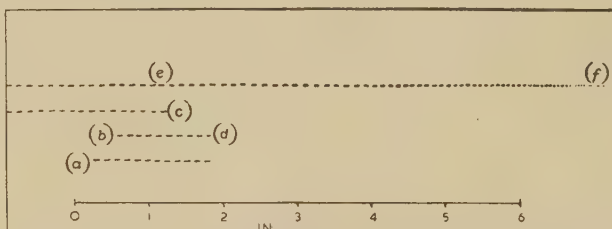


Fig. 2.—A typical trace.

- (a) Emergency switch closed, 0.00sec.
- (b) Relay operated, 0.02sec.
- (c) Micro-switch opened by movement of brake shoes, 0.07sec.
- (d) Power supply cut from motor, 0.12sec.
- (e) Timing circuit.
- (f) Mill stopped.

This diagram is a copy of a trace, and has been drawn with greater clarity.

operated and ended when the mill roll had come to rest. The length of the trace was a direct measure of the stopping distance. Determination of velocity by measuring the distance from one dash to another was found to be unsuitable. This method gave a wide scatter of values for two reasons; first, experimental error in measurement owing to difficulty in determining equivalent points on each dash, and secondly, mechanical wear on the transmission which caused rapid but small fluctuations in speed of the mill roll. Measurement over five dashes gave more consistent values, and therefore the mean velocity over each 0.05sec was calculated to enable velocity curves to be drawn. As the velocity decreased the dashes became smaller and eventually could not be counted under the microscope. It was estimated that the total time could be determined to be within 0.1sec, and a reliable velocity curve could be drawn to within 0.2sec before the mill stopped.

## (4) RESULTS

Table 1 presents the detailed results obtained in the investigation. In most cases the figures represent the mean of four tests, but with mills Nos. 1, 3, 7 and 10, the results of a typical test are given, since they were selected for the illustration of detailed velocity curves. Repeat tests have shown that the method is reliable and machine conditions are stable over periods of two or three weeks.

in turn energized or de-energized the coil controlling the brake shoes and at the same time cut the power supply to the motor. The relay operated 0.005–0.03sec after the emergency switch was closed. The delay in this part of the circuit was very small, and most of the total delay before the braking device operated occurred between the relay and the brake-shoe movement. This delay could be due to a combination of electrical and mechanical effects. It could be as small as 0.04sec or as large as 0.19sec.

Table 1

Mill number	Motor energy	Type of brake	Roll diameter	Roll speed		Braking distance		Time delay		Total braking time	Velocity curve
							Angular rotation	(i) To operation of braking device	(ii) To first powerful brake action		
			in	in/sec	r.p.m.	in	deg	sec	sec	sec	
1	150 (a.c.)	Electro-mechanical; coil energized to operate brake shoes	18	14.2	15.0	9.5	60	0.06	0.10	1.3	Fig. 3 [curve (a)] Fig. 3 [curve (b)]
				13.8	14.6	7.1	45	0.07	0.25	0.9	
2	180 (a.c.)	As No. 1	24	15.0	11.9	8.8	42	0.21	0.25	1.0	Like Fig. 3 [curve (a)]
3	200 (a.c.)	As No. 1	26	21.6	15.1	11.2	49	0.10	0.25	0.9	Fig. 4
4	250 (a.c.)	Electro-mechanical; coil de-energized to operate brake shoes	20	14.4	13.7	8.1	46	0.13	0.35	0.9	Like Fig. 4
5	200 (a.c.)	As No. 4	16	13.5	16.1	6.2	44	0.07	0.15	0.9	Like Fig. 3 [curve (a)]
6	200 (a.c.)	As No. 4	16	13.3	15.9	7.1	51	0.06	0.35	0.9	Like Fig. 4
7	50 (d.c.)	Dynamic; dynamo effect with resistance across rotor	16	15.5	18.5	4.7	34	0.10	0.20	0.5	Fig. 5
8	100 (d.c.)	As No. 7	15	9.6	12.2	3.8	29	0.04	0.20	0.8	Like Fig. 5
9	75 (d.c.)	As No. 7	15	10.4	13.2	5.0	38	0.06	0.25	1.0	Like Fig. 5
10	500 (a.c.)	Dynamic braking with forced field	26	14.9	10.9	12.7	56	0.20	0.50	1.4	Fig. 6
11	500 (a.c.)	As No. 10	28	10.8	7.4	8.2	34	0.10	0.35	1.3	Like Fig. 6
12	300 (a.c.)	As No. 10	26	15.2	11.2	12.4	55	0.30	0.55	1.3	Like Fig. 6
13	150 (a.c.)	Plugging (phase reversal)	20	21.6	20.6	12.5	72	0.15	0.20	1.0	Like Fig. 3 [curve (a)]

Time delay (i) measured to nearest 0.01 sec.

Time delay (ii) estimated from velocity curve to nearest 0.05 sec.

The results may be summarized as follows:

(a) The types of brake covered by the tests were:

(i) *Electro-mechanical*.—Electrically operated spring-loaded brake shoes.

(ii) *Dynamic on d.c. motors*.—A resistance connected across the rotor with mains supply disconnected.

(iii) *A.C. motors (synchronous with wound rotor)*.—Forced-field dynamic braking.

(iv) *A.C. motor (induction motor with slip-rings)*.—Braking by "plugging," i.e. phase reversal.

(b) The roll diameters varied from 15 to 28 in.

(c) The surface speeds of the rolls on which the tests were made were in the range 9.6–21.6 in/sec, equivalent to 7.4–20.6 r.p.m.

(d) The braking distance varied from 3.8 to 12.7 in, equivalent to arcs of 29°–72° or 8–20% of one revolution.

(e) The total braking time varied from 0.5 to 1.4 sec.

(f) The velocity curves will be considered in detail later; only a minority showed linear deceleration. Many showed two periods of strong braking separated by an interval during which little or no brake action occurred.

(g) The delay after pressing the emergency switch before the braking device operated was 0.06–0.30 sec, and the delay before actual braking commenced was 0.10–0.55 sec. Table 1 does not show any details of the timing of intermediate steps, but the main features of these will be outlined.

On mills Nos. 1–6 the emergency switch operated a relay which

There was no significant difference in time delay between brake shoe operation caused by energizing a coil and the same operation caused by de-energizing a coil. It should be noted that the former type were a.c. operated and the latter d.c. operated; the direct current was obtained from a rectifier connected into the braking circuit.

On mills Nos. 7–9 the emergency switch opened a circuit through the brake-contactor solenoid, which released the contactor, thus cutting off the power supply to the motor and applying a resistance across the rotor. Table 1 shows that the time delay before the contactor began to move was 0.04–0.10 sec, but a further 0.10–0.19 sec elapsed before the brake became effective.

Mills Nos. 10–12 which were driven by relatively powerful motors were braked by dynamic methods with a forced field. The emergency switch set in motion a complex series of electrical sequences operated by a number of large contactors. The result was a fairly long delay before the field contactor closed; it amounted to 0.10–0.30 sec. A further delay of 0.25–0.3 sec occurred before the roll velocity began to decrease. The total delay was between 0.35 and 0.55 sec, which was equivalent to a distance of up to 8 in on the roll surface. Variation between repeat tests was fairly high on this type of brake. Fortunately these powerful motors were driving large mill rolls revolving fairly slowly, and it was possible to obtain satisfactory stopping distances even with the long delay.

Mill No. 13 was braked by "plugging." The emergency switch operated a relay (delay, 0.01 sec) which in turn caused the reversal of a pair of phases, and thus set the motor into reverse. The total delay before the reversing contactor closed was 0.15 sec, and braking commenced 0.05 sec later. In this case the mill stopped, and then, on reversing, the power supply was cut off by a suitable action switch attached to the shaft of the mill. The result was that the roll rotated one-half revolution in reverse before finally stopping.

#### (4.1) Velocity Curves

After detailed examination of examples of brake action, four separate cases have been selected:

- Electro-mechanical having ideal action initially, but showing a delay after adjustment (Fig. 3).
- Electro-mechanical showing delay before the brake acts (Fig. 4).
- Dynamic on a d.c. motor (Fig. 5).
- Dynamic with forced field on an a.c. motor (Fig. 6).

Fig. 3 (mill No. 1) shows the velocity curve for an electro-mechanical brake operating on a 150h.p. a.c. motor driving a line

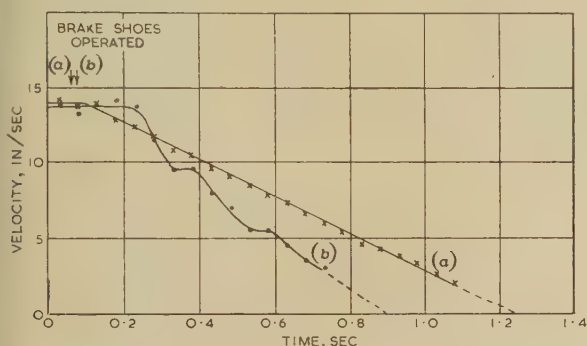


Fig. 3.—Mill No. 1. Electro-mechanical brake.

- May, 1953.
- November, 1953, after increasing spring tension.

consisting of one 18 in, one 12 in, two 14 in, and two 17 in diameter mills. The emergency switch energized a relay which in turn energized the trip coil releasing the brake shoes. The spring-loaded brake shoes then moved rapidly into contact with the brake drum. Two curves are shown—curve (a) is the result of a test carried out in May, 1953, and curve (b) shows a test carried out in November, 1953. Between these dates the stopping distance had slowly increased from 9.5 in in May to 10.6 in in July, and eventually exceeded the limit of 11 in set for this particular mill. About three weeks before the November test the spring tension had been increased to reduce the stopping distance, which was reduced down to 7.1 in. Tests carried out in May [curve (a)] and July gave a linear deceleration curve without much delay after the brake shoes had moved, but in November the test showed a pronounced delay and also two steps in the deceleration curve. The delay before the relay operated was 0.01 sec in May with the brake shoes moving after 0.06 sec. In July and November the delay before the relay operated was 0.02 sec, and the brake shoes moved after 0.07 sec. The roll began to slow down after 0.10 sec in May, 0.15 sec in July and 0.25 sec in November. Curve (a) represents an ideal condition giving minimum strain on the motor and transmission.

Fig. 4 shows the test results on mill No. 3 equipped with a 200h.p. a.c. motor having an identical brake arrangement. This motor was driving two 26 in mills. The delay before the brake shoe moved was 0.09 sec, but a further 0.16 sec elapsed before the brakes began to act. If the shoes were in contact with the drum during this period, and it certainly appears that they

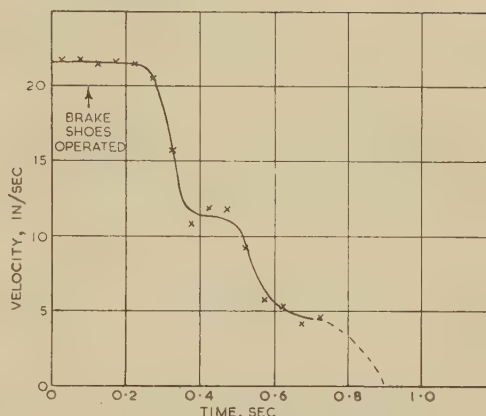


Fig. 4.—Mill No. 3. Electro-mechanical brake.

were, the drum revolved  $1\frac{1}{2}$  times before the shoes began to grip. It may be significant that all cases which show this initial delay after the brake shoes have contacted the drum also show two or three periods of strong deceleration separated by a period or periods of almost negligible brake action.

The deceleration curve for a dynamic brake obtained on a 16 in roll mill driven by a 50h.p. d.c. motor is shown in Fig. 5

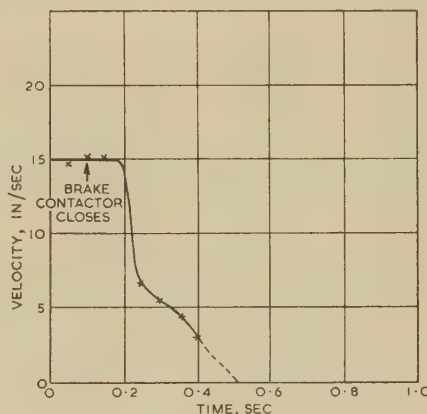


Fig. 5.—Mill No. 7. Dynamic brake. D.C. motor.

for mill No. 7. This illustrates the severe initial deceleration, followed by a tailing-off of the brake action. The jolt was both visible and audible; there was some lateral play in the bearings of this mill and the roll usually moved sideways at the instant of this severe retardation. At the instant that the brake took effect the velocity reduction was so sudden that it was misleading to plot a mean velocity at each interval of 0.05 sec, and the fine structure of the trace was examined to obtain the correct change in gradient.

Fig. 6 shows a velocity curve obtained on mill No. 10—one of three 26 in mills driven by a 500h.p. a.c. motor. This motor was dynamically braked by a forced field, and the contactor closed after 0.2 sec; a further 0.3 sec elapsed before any deceleration was measured. The velocity curve shows a long delay, followed by two periods of fairly strong brake action separated by a fairly long interval of weak brake action. A long delay is characteristic of this type of brake. After the field contactor closed, the resistance across the stator was reduced in three stages controlled by a timing relay. Two stages are shown on the velocity curve by arrows. The final reduction of resistance occurred after the motor had stopped, and it appears that the third stage is not effective on this particular machine.

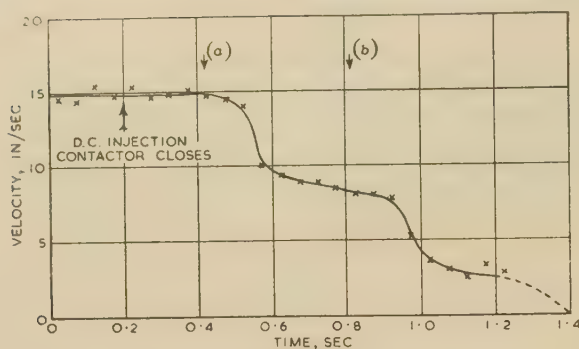


Fig. 6.—Mill No. 10. Dynamic brake with forced field.

- (a) First reduction in resistance across stator.  
(b) Second reduction.

The third and final reduction occurred after the mill had stopped.

The shape of the velocity curve may possibly be associated with the stepwise reduction of resistance; the shape is similar to the curves obtained from electro-mechanical brakes showing uneven deceleration (compare Fig. 6 with Fig. 4). Insufficient work has been carried out to state whether this is purely accidental or whether some mechanical feature of the transmission causes these similarities in velocity curves from two entirely different types of brake.

#### (5) RATE OF DISSIPATION OF ENERGY

Three kinetic energies were calculated to illustrate the energies to be absorbed by the braking system. A detailed calculation of a typical arrangement of motor and mills showed that 90% of the energy was stored in the motor itself, 7% in the gear-box and the remaining 3% in the relatively slow-moving shafts, gears and mills rolls.

For mill No. 3 (Fig. 4) the kinetic energy was 53 000ft-lb; for mill No. 7 (Fig. 5) it was 13 000ft-lb; and for mill No. 8, 26 000ft-lb. The energy seems to bear a proportional relation to the energy of the motor, in that 100h.p. is equivalent to 26 000ft-lb, or 1h.p. is equivalent to 260ft-lb. When considering these powers and energy distribution, it should be realized that the general practice is to allow a considerable margin of power in a motor driving two-roll mills to enable the initial breakdown of the raw rubber to be handled without serious overload. When the brake was operated mill No. 7 continued to run at full speed for about 0.2sec until the dynamic brake took effect. In the next 0.05sec the velocity decreased by more than half, and thus over three-quarters of the kinetic energy had been dissipated in this very short period. This rate of energy absorption is equivalent to a braking power of about 450h.p. acting for 0.05sec on a 50h.p. motor.

#### (6) DISCUSSION AND SUMMARY

The method of determining braking distance and timing the sequence of operations using Teledeltos paper has enabled the time to be measured to within 0.01sec, and has given a permanent record of brake action capable of analysis in distance and time. Repeat tests have been most satisfactory and indicate the reliability of the method. All the mills tested had been adjusted to give a satisfactory stopping distance, from the point of view of safe operation, before any of these studies were made. The results represent actual operating conditions in the factory.

Substantially linear deceleration was found only on electro-mechanical and "plugging" brakes. Some electro-mechanical brakes showed a time delay before the brake shoes began to grip, which is the phenomenon known as "brake glide."

Coupled with this delay there was a typical deceleration curve showing variations in braking power. On one machine, both types of brake action were obtained with different tensions on the brake-shoe springs. It appears, therefore, that glide and deceleration curve shape are somehow associated with pressure on the brake shoes.

Dynamic braking of d.c. motors was found to be very severe at the onset of the braking period, followed by a reduction in braking action as the velocity decreased. Dynamic braking with a forced field showed large time delays before effective braking commenced, followed by variations in braking power as the motor slowed down. Some variation between tests was due to contactor bounce delaying the sequence of electrical contacts, and this delay varied from test to test.

If the following important points are considered:

- (a) Time delay before onset of braking.
- (b) Strain on motor and transmission during braking.
- (c) Smooth linear reduction of velocity.
- (d) Ease of adjustment of brake action.

it would appear that the electro-mechanical system is the best for emergency brakes on two-roll mills as described in the paper. These are typical of the rubber industry. Attention to the minor problem of brake glide, and the resulting uneven deceleration which appears to be associated with it, would result in a type of braking action which would meet all requirements for safe working on two-roll mills.

#### (7) OBSERVATIONS ON THE APPLICATION OF BRAKING TWO-ROLL MILLS

The following items should receive attention when designing a system for two-roll rubber mills. They are as follows:

(a) All control circuits should "fail to safety," i.e. failure of supply or operation of a fuse or any similar occurrence should cause the brakes to be applied. This tends to rule out the use of electrically-operated mechanical latches, in which the latch is tripped by operation of the supply to a small solenoid, and also the use of "plugging" and forced-field methods.

(b) Time-constants of all devices in the circuits should be reduced to a minimum.

(c) Kinetic-energy systems should be reduced to a minimum. From Section 5, it will be seen that, of the mills tested, by far the greater part of the stored energy resided in the rotor of the motor. When designing *ab initio*, much can be done to reduce the kinetic energy. The ratio of length to diameter of the typical motor rotor is usually less than unity, but it can be increased to between two and three. Furthermore, if a separate motor is used for ventilation instead of the normal shaft-driven fan, the kinetic energy can be further reduced, and also improved ventilation will make tolerable a greater ratio of length to diameter. By these means the kinetic energy can be reduced by a factor of about 4.

(d) It will follow from the reduction of the operating and delay times that the size of the drum for mechanical systems can be reduced, and this in itself will bring about further considerable reduction in the kinetic energy, for the stored energy of the brake drum is second only to that of the rotor in most systems.

From the results it can be seen that considerable attention must be paid to the timing of the operation relays if the regenerative braking systems for these are to be used.

From Fig. 6 it can be seen that delays of about  $\frac{1}{2}$ sec and total stopping times of  $1\frac{1}{2}$ sec have resulted from the time-constant of the solenoid and field systems, and in fact the last contactor for short-circuiting the resistance only functioned after the rolls had stopped. Application of the methods outlined in the paper to machines, and careful consideration of the results

obtained, should be of considerable assistance to the motor and control-apparatus designer.

The result of using the dynamo effect shown in Fig. 5 is of particular interest. This illustrates strikingly the effect of the time-constants of the machine. The time-constants of the relays are small, but there is considerable delay before any effective braking is applied. The extreme violence in the initial stage is marked; in approximately 1/20 sec some 75% of the kinetic energy is dissipated. This must result in severe stressing of the

transmission equipment, as well as mechanical stressing on the insulation on the rotor windings.

#### (8) ACKNOWLEDGMENTS

The authors would like to express their gratitude to the Leyland and Birmingham Rubber Co., Ltd., for providing facilities to carry out experiments on their equipment and to Mr. R. W. Lunn for his encouragement in the work.

### DISCUSSION BEFORE THE MERSEY AND NORTH WALES CENTRE, AT LIVERPOOL, 24TH JANUARY, 1955

**Mr. J. O. Knowles:** The authors' method of checking the emergency stopping distance for rubber rolls can be applied easily and externally, i.e. without disconnection of any mechanical or electrical features of the mill. It gives an indisputable record of the travel of the roll surface between the operation of the sensitive guard and the rolls coming to rest.

The fact that the velocity of the roll surface in the last 0.2 sec is not accurately recorded is of small importance. What matters is the stopping distance.

My instinct is to be suspicious of a braking system in which the brake shoes are held off by a latch released by a solenoid magnet. The authors state that this system does not fail to safety, and I can only suppose that this objection is the basis of my instinctive dislike of it. Properly adjusted, the latch will, no doubt, be released by the solenoid as certainly as a shunt strip will open a circuit-breaker, but brake shoes wear and the readjustments might reduce the factor of safety.

Allowance for wear is a big factor in the design of brakes. I know of an organization which is spending many thousands of pounds in redesigning their electrically operated brakes. One of the most important features is a redesign of the hinge of the brake shoe to give longer wear. Another important feature commented on by the authors is the kinetic energy of the brake drum. The steel industry in this country and in America is asking for brake drums with less kinetic energy—and it is going to get them.

The authors' analysis of the components of kinetic energy is as follows:

90% in the motor.  
7% in the gearbox.  
3% in the rolls and slow-moving parts.

How much of the 90% is in the motor rotor and how much in the brake drum?

I am puzzled to find no reference in the paper to clutch brakes. There are, of course, some difficulties in applying plate clutches to drives of large horse-power. The driven and driver shafts must obviously be lined up accurately and correctly applied. Motor-car clutches have to transmit 200 h.p. and more without slipping; at least, some slip and some do not.

I can visualize a clutch brake, i.e. a combination of clutch and brake, which had no electrical features at all. Why could rubber mills not be provided with a clutch which held only as long as hydraulic pressure was available (i.e. failure to safety), and which, as soon as hydraulic pressure was released, opened the clutch, thereby removed the source of most of the kinetic energy (the driving motor) and let the brake take hold of and stop the driven members? Is there no such scheme, and what are the difficulties?

The authors suggest that the kinetic energy of the driving motor can be reduced by designing it bearing this factor in mind. At least one American motor manufacturer produces d.c. motors specially designed to provide not only speed variations, but rapid variation of speed, rapid acceleration and rapid braking.

I do not believe that the authors are fair to d.c. dynamic braking schemes. In the particular instance they give, the initial braking resistance is too low, and further, the resistance is cut out by steps controlled by time instead of current. It is quite possible to sustain the braking torque by cutting out resistance as the motor slows down, thus maintaining the braking current at any desired value as the motor slows down.

Schemes of current control of this kind are, when once set, self-adjusting—unlike mechanical braking systems. But they should be current-controlled, and not time-controlled.

The authors are afraid of the mechanical stressing of the insulation of the motor windings (stator or rotor or both) by high braking torques. So was I, until I had experience of stopping motors in the textile industry in one revolution or less—from full speed. An ordinary squirrel-cage motor can be stopped in one revolution electrically, by injecting sufficient direct current into the stator windings, through a short-rated rectifier—and it appears to withstand this treatment repeatedly for years. I think the answer must be that such electrical stopping is as 'cushiony' as the hydraulic reversal of a grinding-machine table.

I should like to add a further note on the time of operation of contactors, particularly d.c. ones. If a d.c. contactor coil has a resistance—or a lamp in parallel with it—and the coil current is broken *outside* these parallel circuits, the time of opening will be many times longer than if the circuit is broken *inside* these parallel circuits. This is a well-known device used in control circuits, but now and again some circuit designer falls into this trap. With large d.c. contactors, a time lag of even several seconds can be so obtained, if desired.

The times of operation of contactors can be of the following order:

		Closing	Opening
50 amp d.c.	.. ..	0.14 sec	0.06 sec
150 amp d.c.	.. ..	0.175 sec	0.08 sec
50 amp a.c.	.. ..	0.05 sec	0.03 sec
150 amp a.c.	.. ..	0.05 sec	0.05 sec

Contactors can be normally open or normally closed, the latter being back-ended contactors, which close when their coil circuit is interrupted. In braking circuits no fuses are used—the current must flow until the motor stops. Electrical braking circuits can therefore be arranged so that they fail to safety.

**Mr. M. A. McTaggart:** The problem of stopping the mill is really one of stopping the motor, and viewed from all angles, the ideal appears to be a combined clutch brake, which would disconnect the energy in the motor from the rest of the system, and at the same time brake the relatively small energy in the rolls and gearing. This ideal has been realized on two roll mills by the use of combined clutch brakes, some of which are operated by compressed air, and others electrically.

From Fig. 1 it will be seen that the braking sequence is initiated by a mechanical movement of a sensitive safety bar, and the final movement is the mechanical one of the brake shoes. The authors have shown that, in the interim, there is an appreciable

time delay in the operation of the solenoid and contactors. It seems to me that this time delay could be eliminated entirely by the substitution of a mechanical connection between sensitive bar and brake. The brake would have to be pulled by hand or other means to the off position and held there against the weights or springs by a simple latch, which would be withdrawn by the operation of the sensitive safety gear. The idea is attractive, because at the sensitive safety bar, a movement of about  $\frac{1}{2}$  in, together with a thrust of 40 lb, is available to operate the latch.

The energy in the drum of a solenoid friction brake is probably several times that in the transmission and rolls, and in this connection an interesting case was brought to notice. It was desired to equip an existing unbraked mill with such a brake. The braking distance that could be obtained was estimated for each size of brake available. It was found that the stopping distance did not continue to decrease with brake size, but showed a minimum turning value, after which a larger brake only served to increase the stopping distance. The conclusion is that, with existing designs, there is a minimum obtainable stopping distance.

When discussing brake preference, it must be stated that maintenance is all-important with solenoid friction brakes. Time after time these brakes have been found to allow the stopping distance to increase from one-eighth to one-half or even one revolution in a week or two. There had been, of course, serious neglect of maintenance and test.

With regard to electrical brakes, the authors have revealed sizeable delays in the operation of the contactors. It would seem possible to arrange these contactors in the form of a drum controller, suitably driven to reduce the delays in the sequence. The plug brake does not, of course, fail to safety, but the contingency of the operator being in difficulty at the same moment as the power fails is judged to be somewhat remote. Where direct current is necessary for braking, a continued supply sufficient for braking purposes can be obtained from a suitably rated flywheel motor-generator set.

In the report of the Engineers Advisory Committee two points were stressed. First, the brake must be reliable, i.e. it must work every time; and secondly the brake performance must be constant. If it is set to stop in a distance of 6 in and it stops in 8 in, the operator may lose some fingers; if it stops in a greater distance the operator may lose a hand, and so on. Maintenance and test are all-important.

**Mr. F. D. H. Bremner:** Teledeltos paper was originally developed as a recording medium for facsimile work in ordinary message communication, i.e. for the legible and recognizable reproduction of handwriting, signatures, typescript, small sketches, mathematical symbols, etc., none of which requires a very high definition or a high stylus speed over the paper. This is the primary interest of the makers, and so we have carried out no research work on the behaviour of Teledeltos paper where considerable accuracy is required in the position of a mark on the paper or where high writing speeds are involved.

The authors appear to obtain the time basis by noting the point at which the paper breaks down, and they assume that this represents a constant point on the voltage/time curve of the source of supply, but I am not sure that this is necessarily the case. In

the first place, I have no figures to show whether or not the paper and its coating are both so uniform that the breakdown voltage is constant at all points on the paper. But I do know that the breakdown voltage does vary with the writing speed, increasing with increasing speed, and I have a suspicion that it may also vary both with the stylus pressure and the amount of carbon which has accumulated on the tip of the stylus. Furthermore, when writing at high speed the trace consists of a series of dots, each one of which is smaller than the end of the stylus, and the point of breakdown may not necessarily be at a constant point on the stylus. I doubt whether any of these variations are of sufficient magnitude to affect the results obtained by the authors, but they refer to use of a travelling microscope, which gives the impression that they are working to very fine limits.

I have referred to writing at high speeds. The maximum velocity shown on the authors' curves is about 22 in/sec, whereas Teledeltos can, under suitable conditions, write up to a speed of more than 400 in/sec. To obtain good results at this speed, it is necessary to match the stylus output to the impedance of the paper, and it would probably need an open-circuit voltage of about 1 000 volts with a series resistance of some 200 000 ohms. If accurate timing were required, I would prefer to use a stylus deflected by a 50 c/s voltage and having an amplitude of 1 or 2 in. This would give a writing speed within the capabilities of the paper, and would give a measure of time almost as accurate as the frequency of the voltage.

I would like to make it clear that such figures as I have quoted have been obtained from discussions with people who have themselves done experimental work on the particular usage which they have for Teledeltos paper, and not as a result of our own research.

**Mr. R. W. Lunn:** In the early stages we were only concerned with finding devices by which we could work within the limits laid down by the principles involved. We set out to work within certain limits and were not concerned at that time about what might be happening other than within those limits. We were very fortunate in finding there were means to hand and that there were easy methods of making the means which were not necessarily immediately to hand, by which we provided ourselves with the equipment needed to safeguard the mills in the rubber industry. If we had not had the good fortune to find methods within the limits, we might have had considerable difficulty in making the mills safe.

Other industries suffer from nip accidents quite as devastating as those in the rubber industry, e.g. the steel industry. The rubber industry laid down certain principles upon which 2-roll mills could be made safe, and these could be extended to cover other 2-roll systems, which may be responsible for terrible accidents. Other industries may not be so fortunate, but in any case the overriding factor is *time*. The authors give an explanation of a method of studying the time factor in detail. I hope that the technique will be extended beyond the rubber industry to produce safe conditions on other machines which can cause devastating nip accidents.

[The authors' reply to the above discussion will be found on page 120.]

## UTILIZATION SECTION, 17TH NOVEMBER, 1955

**Mr. O. I. Butler:** I should be glad if the authors would state their reasons for using a sequence of three values of resistance in applying d.c. dynamic braking to mill No. 10 (see Fig. 6).

It is evident that, besides the delay and complication involved in switching from one resistance to another, further delays dependent upon the time-constant of the secondary circuit are introduced at each switching step. It can be shown that a fixed

value of  $R_2 = (X_2 + X_m)/\sqrt{3}$  produces the minimum braking distance and approximately the minimum braking time. In fact, it results in a braking time almost as small as the 'ideal' value, i.e. the value obtained when the machine operates at its maximum braking torque for the whole of the braking time. The value of  $X_m$  should be the effective value under saturation conditions, which may be readily determined by the mathematical

method of analysis referred to in my contribution to the discussion on Dr. Harrison's paper.\* I would suggest, therefore, that the d.c. dynamic braking method has not been used by the authors to the best advantage.

I further suggest that the method of plugging has not been used to the best advantage. It can be shown that a fixed value of  $X_2 = (X_1 + X_2)\sqrt{3}$  produces the minimum braking distance and approximately the minimum braking time. In fact, it can result in a closer approach to the 'ideal' braking time than the d.c. dynamic braking method. Also, if the drive is not damaged by a light reverse rotation, the person trapped between the rolls is automatically released by the plugging method.

The application to a.c. motors of the d.c. dynamic and plugging methods, with the optimum conditions given above, would provide results much superior to those given by the authors in Table 1. In particular, since the energy dissipation in the motor during braking is unimportant owing to infrequent operation, I anticipate

that plugging is preferable both to the electro-mechanical and d.c. dynamic braking methods, when applied to the best advantage.

With regard to the doubt expressed by the authors in the last sentence of Section 4.1, the similarity between Figs. 4 and 6 must surely be due to the inherent characteristics of the alternative braking methods, as applied by the authors.

**Dr. H. G. Taylor:** With regard to the start-and-stop action of the brakes noticed by the authors, it seems that it is very similar to 'brake fade' on motor cars, the explanation of which is identical to that given by the authors. When the brakes are strongly applied, the resultant temperature rise causes evaporation of resinous material from the brake lining, which acts as a lubricant to the braking surfaces. According to the Motor Industry Research Association the complete solution to this trouble is to heat the brake linings prior to use to such a temperature that all the resinous bonding material is driven off.

#### NORTH-WESTERN UTILIZATION GROUP, AT MANCHESTER, 15TH DECEMBER, 1955

**Mr. J. N. M. Legate:** The data provided in the paper show very clearly the necessity for minimizing lags in series when critical stopping times are required on grounds of safety. It should, of course, be possible to reduce the effective inertia of the system and so render the braking requirements less onerous, either by the use of a specially-designed low-inertia motor or possibly by lower-speed machines with smaller-reduction gear ratios. Possibly cost is the objection.

The method used for obtaining records of displacement and time intervals seems to be very convenient for many applications other than the specific ones referred to in the paper. The type of application where it might be useful could include the timing of intermittent motions in small mechanisms, where a light stylus could be attached to the mechanism without introducing any serious inertia or frictional loads. Have the authors any experience of such practical uses, e.g. the investigation of relay, contactor and brake lags?

The very convenience of the method may have some dangers in that a 'lash-up' could very easily be arranged, and it might be considered that on this account precise measurement and adequate records for future reference are unnecessary. I do not suggest that this attitude has occurred in the authors' work, but merely that the type of information obtainable by this method should be of sufficient use to justify care in the accuracy and recording of results, as similar information by the use of oscilloscopes and high-speed pen recorders is not always easily obtained. Incidentally, are the sensitized-paper records as permanent as those obtained from an orthodox pen recorder?

**Mr. A. G. Clothier:** The authors have concluded that mechanical brakes are inherently more reliable than any purely electrical method. Could it be that the initial outlay on the first method recommends it to the user? The question of the reliability of an electrical braking system is largely a matter of the correct enclosure for the contactor gear and attention to the design of the control circuits in order to ensure that the emergency circuits take priority over any other signal. The possibility of an accident coinciding with a failure of supply would be very remote.

A further form of emergency brake which has been successfully applied to lines of mills is the electromagnetic clutch brake. When supplied from a small generator driven from the main motor, it is assured of continuity of supply. This method disconnects the main source of inertia from the system and obviates the need for special motors.

I am surprised that the plugging brake took the greatest distance of all to bring the mill to rest. This method is frequently

applied to laboratory mills which have high roll speeds, but owing to their small size the material can be manipulated at greater angular distances from the nip.

The very high torques necessary to stop the mills driven by d.c. motors are remarkable, since an average braking torque equal to twice the full-load torque is usually sufficient to achieve the correct braking distance.

**Mr. E. R. Laithwaite:** The timing mechanism described resembles in some ways the 'falling-plate' method of measuring  $g$  described in most elementary textbooks of physics, and it is about as accurate. Fig. A shows the type of results which can

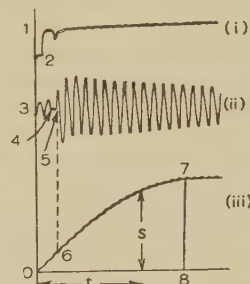


Fig. A

be obtained using standard electronic apparatus. In the particular case shown an induction motor is being brought to rest by plugging. The starting end, 1, of each trace corresponds to the instant at which the mechanical trip is operated. Trace (i) shows the current in the relay coil. The dip at 2 is thought to be due to the relay armature closing the magnetic circuit. Trace (ii) represents the line current to the motor. The small-amplitude wave from 3 to 4 is the load current. The flat portion from 4 to 5 is clearly the relay-contact travel time, and the subsequent large-amplitude wave is the plugging current, including transient. Curve (iii) shows the distance travelled,  $s$ , after any time  $t$ . The trace, in fact, represents the voltage across a potentiometer driven by the motor shaft. The trace is straight from the origin to 6, where the braking current begins. At 7 the machine has stopped. 0-8 is the time taken to stop, which can be derived by comparison with the 50 c/s waves of trace (ii); and 7-8 is the distance traversed during that time, which can be calculated from a knowledge of the potentiometer calibration.

These traces were photographed with a standard camera fitted

\* HARRISON, D.: 'The Dynamic Braking of Induction Motors' (see page 121).

to a commercial cathode-ray oscillograph. Although the apparatus is more complex than that described by the authors, it is probably more readily obtainable since it is standard equipment, and requires no additional 400 c/s supply in order to achieve an accuracy of 1/1000 sec. Furthermore, it provides additional information in the sense that the actual waveforms of current and voltage are displayed.

What advantages has the method adopted by the authors over simple electronic methods?

**Mr. G. V. Sadler** (*communicated*): It would be useful if some mechanical details of the electro-mechanical brakes were included. Is the weight of the solenoid plunger and linkage added to the

brake-spring pressure when the brake closes, or, by means of a 'loose-coupled' linkage, is the braking force entirely dependent on spring pressure? There is a distinct difference in braking characteristics under these two systems.

Whilst the theories put forward to account for brake glide are interesting, the phenomenon may also be caused by solenoid plunger bounce and slackness in linkage pins in the rigid form of linkage mentioned above. Another cause is due to a brake being slightly undersized for the job it is expected to do.

If the mechanical details of the brakes used were stated by the authors, this last point could be checked since it is not uncommon in braking systems.

## THE AUTHORS' REPLY TO THE ABOVE DISCUSSIONS

**Messrs. C. Cuthbert and D. A. Picken** (*in reply*): Many speakers refer to difficulties experienced with braking systems, but in our experience with such systems regularly used for stopping machines at least once per day for test purposes, together with any occasions when inadvertent or unexpected use occurs, it is usual for the brakes to operate without adjustment for about six months—and occasionally for as long as 18 months. In the worst case, under particularly onerous conditions working three shifts, the braking distance increased by about 5% per week and necessitated adjustment every three to five weeks.

Mr. Knowles is puzzled to find no reference to clutch brakes, and other speakers also observe that we have not referred to particular systems of braking. The purpose of the paper was to demonstrate the technique of timing control systems, and tests were carried out on a wide variety of braking devices which happened to be available in one particular factory. This information has been supplemented by general information obtained from other works, but it does not claim to be a comprehensive survey of all braking systems.

The suggestion of a clutch brake, with hydraulic features failing to safety, is a good one, and in fact a similar system is in use, which is air-operated.

Mr. Knowles states that clutches in motor cars transmit up to 200 h.p. This is true, but the torque conditions are very moderate compared with rubber rolls for, whilst a motor-car engine only develops a peak horse-power of 200 h.p. at 4 500–5 500 r.p.m., the motors driving rubber rolls not infrequently momentarily develop about 1 000 h.p. at speeds of 250–500 r.p.m.

In connection with Mr. McTaggart's reference to plugging brakes, it is not unknown for the high current taken during plugging to operate the protective fuses and so leave the mill without braking. A further difficulty, which has occurred with dynamic brake systems, is that, if a worker has been trapped, one of his colleagues, seeing him in difficulties, has pressed the stop button for the motor before the trip bar has operated, and this has eliminated all braking effort.

Mr. Bremner refers to the accuracy of the system, and we can only state that repeated tests of the same machine follow exactly the same pattern, so that, if there is any random variation, it is of very small magnitude and quite negligible for the purpose for which this method is required.

Mr. Butler suggests improvement of the plugging. It was in order to emphasize the necessity for such considerations that we gave rather more prominence to these problems than was strictly necessary for the purpose of demonstrating the technique.

Dr. Taylor mentions the practice of baking brake linings in order to drive off the resinous bonding material. Whilst this might be practicable for metallic-asbestos brake linings, it is not permissible for cotton-woven ones, which are necessary to give the required coefficient of friction with the normal design of drum brake.

The suggestions made by Mr. Legate for incorporating a stylus have been utilized, but the difficulty lies in co-ordinating the timing of the various components.

Mr. Clothier comments that the high torques used to stop the d.c. mills are remarkable, and it is agreed that a twice full-load torque would have given much better braking had it been available during the whole time of braking. It is because of the marked attenuation of braking effort resulting from the dynamic brake that high initial values have to be achieved in order to give the overall stopping distance within satisfactory limits.

Mr. Laithwaite comments that the method is about as accurate as the elementary technique used for measuring  $g$ . We cannot comment on this, but we do appreciate that, provided that the elements are fundamentally sound—which we think applies in the case of our technique—there is no great virtue in elaboration.

We have used techniques similar to those mentioned by Mr. Laithwaite to produce Fig. A, and they have undoubtedly been used. However, the application of elaborate expensive apparatus and delays whilst results are developed and made available compare unfavourably with the simplicity of the technique we have adopted. This produces instantaneously-permanent legible traces requiring no more interpretation than does Fig. A, which also requires a travelling microscope for reasonable interpretation.

Undoubtedly our method has its limits, but within these limits we should be very pleased to learn of a better technique. The only way we imagine that Mr. Laithwaite could determine the advantages of this technique over 'simple' electronic methods would be for him to try out the technique, the equipment for which we are sure will be found in the 'junk box' in the corner of the laboratory.

In reply to Mr. Sadler, the brakes which we tested were entirely loose coupled. With reference to his comment that bounce or slackness in the linkage pins could be the cause of brake glide, careful investigation was made, and we are satisfied beyond reasonable doubt that the causes for the delay did not arise from mechanical considerations but were a function of the nature of the braking material.

# THE DYNAMIC BRAKING OF INDUCTION MOTORS

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## SUMMARY

A simple graphical construction is developed for predicting the dynamic braking torques of slip-ring and squirrel-cage induction motors, taking full account of saturation and of the rotor leakage reactance.

The test results given confirm the accuracy of the method. It is shown that the rotor-circuit reactance may often be neglected where great accuracy is not required.

The proper way of calculating the total deceleration time of an induction motor under dynamic braking conditions is also given.

## (1) LIST OF PRINCIPAL SYMBOLS

- $V_1$  = Stator supply voltage per phase, volts.  
 $V_2$  = Rotor e.m.f. per phase, at angular frequency  $\omega_s$ , volts.  
 $I_1$  = Stator current per phase, amp.  
 $I_2$  = Rotor current per phase, amp.  
 $I_m$  = Magnetizing current per phase, amp.  
 $I_D$  = Direct stator excitation current, amp.  
 $X_1, X_2, X_m$  = Stator leakage reactance, rotor leakage reactance and magnetizing reactance, respectively, expressed in ohms per phase at the angular frequency  $\omega_s$ .  
 $R_1$  = Stator resistance per phase, ohm.  
 $R_2$  = Rotor circuit resistance per phase, ohm.  
 $R$  = Equivalent rotor circuit resistance per phase, ohm.  
 $\omega_s$  = Unit angular frequency or synchronous speed, electrical rad/sec.  
 $\omega_1$  = Stator supply angular frequency, rad/sec.  
 $\omega_2$  = Rotor speed, electrical rad/sec.  
 $N$  = Rotor speed, r.p.m.  
 $\Phi$  = Air-gap flux.  
 $\phi_2$  = Phase angle between rotor e.m.f. and current.  
 $T$  = Torque in synchronous watts per phase at synchronous speed  $\omega_s$ .

## (2) INTRODUCTION

In the paper the term "dynamic braking," as applied to induction motors, refers to that system of electric braking in which the stator windings of the motor are excited by direct currents, so that the machine becomes an alternator. The alternating voltages generated in the rotor windings cause currents to flow in the rotor circuit, the power dissipated in the rotor-circuit resistance constituting the braking power. The resistance may be that of the rotor winding only, necessarily so in a squirrel-cage motor, or may include external load resistance in a slip-ring motor.

This method of braking is widely used for induction-motor-driven mine winders, and also for the quick retardation of machines of high inertia driven by induction motors. The advantages over the reverse-current or "plugging" method are that no rotation in the reverse direction is possible and that the

energy dissipated in the rotor is very much less. The disadvantage is that a source of direct current must be provided.

The accuracy with which it is desirable to predict the braking torques depends on the particular application. Where it is merely a case of stopping a squirrel-cage motor in a reasonably short time, high precision may not be necessary. On the other hand, for mine winders, particularly if automatic winding is to be used, the braking torques must be calculated as accurately as possible. Here it is necessary to take account of saturation and of the rotor leakage reactance.

In a previous paper<sup>1</sup> the author described a method of calculating dynamic braking torques, neglecting the rotor leakage reactance, which gives quite good results and may be accurate enough for many purposes. This method has recently been applied to squirrel-cage motors,<sup>2</sup> with fairly good results. Other authors have used "adjusted synchronous reactance" methods,<sup>3,4</sup> but these are either tedious to apply or rather inaccurate. In view of the operating conditions, with speeds ranging from zero to synchronous speed and loads nearly always resistive, the methods used for normal alternators working at constant speed, with loads of varying power-factor, are not the most convenient. The method described in the paper, which is based on the well-known techniques used for induction motors, is simple and straightforward, and takes full account of saturation and of the rotor leakage reactance. Comparison of the calculated and measured results indicates the high order of accuracy obtainable.

## (3) THEORY

### (3.1) Equivalent Circuit

The equivalent circuit of one phase of a polyphase induction motor is shown in Fig. 1, in a rather more general form than is

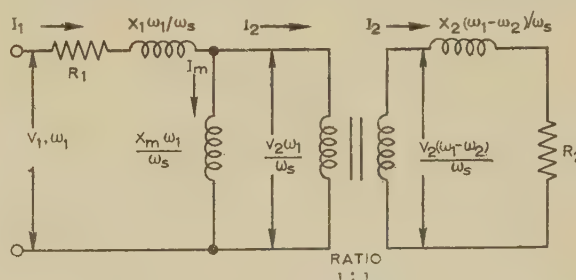


Fig. 1.—General circuit of induction motor.

Stator frequency,  $\omega_1$ .  
 Rotor frequency,  $(\omega_1 - \omega_2)$ .

usually adopted. The quantities  $V_2$ ,  $X_1$ ,  $X_2$  and  $X_m$  are all based on the unit angular frequency  $\omega_s$ , while the stator supply is at angular frequency  $\omega_1$ , the rotor speed is  $\omega_2$  and the rotor currents are of angular frequency  $\omega_1 - \omega_2$ . (This kind of generalization is described in more detail in Reference 5.) For simplicity, the stator/rotor ratio is taken as unity, i.e. all quantities are referred either to stator or rotor, whichever is the more convenient. Fig. 1 shows the actual values of the e.m.f. and the reactances for the assumed conditions, and Fig. 2 shows the

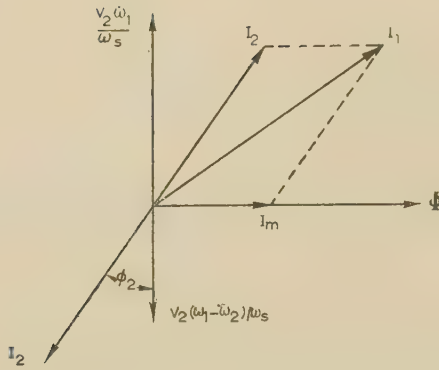


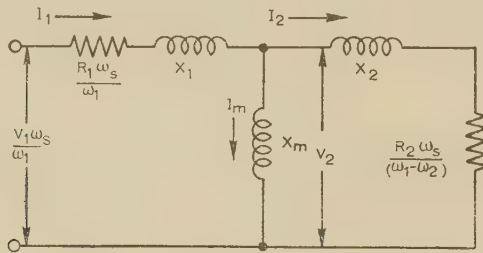
Fig. 2.—Vector diagram of induction motor.

corresponding vector diagram. The stator iron loss is neglected, so that  $I_m$  is cophasal with  $\Phi$  and in quadrature with  $V_2$ . This is fully justified when the stator current is direct, i.e. when  $\omega_1 = 0$ .

The torque exerted on the rotor is proportional to  $\Phi I_2 \cos \phi_2$  (Reference 6), and since  $\Phi$  can be taken as proportional to  $V_2$ , the torque expressed in synchronous watts per phase, based on the synchronous speed  $\omega_s$ , is given by

$$T = V_2 I_2 \cos \phi_2$$

The equivalent static circuit is then derived by dividing the rotor resistance, reactance and e.m.f. by  $(\omega_1 - \omega_2)/\omega_s$ , so that the actual rotor circuit is replaced by one with the same flux linkage but with angular frequency  $\omega_s$ . The stator circuit is also modified similarly, dividing its voltage, resistance and reactances by  $\omega_1/\omega_s$ . This leaves the currents and phase angles unchanged. The ideal 1:1 ratio transformer may then be eliminated to give the resultant circuit shown in Fig. 3, in which the angular frequency throughout is  $\omega_s$ .

Fig. 3.—General equivalent circuit.  
Frequency,  $\omega_s$ .

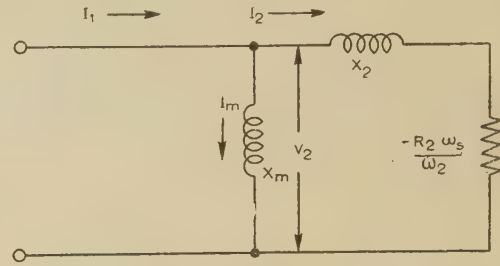
The torque per phase,  $T$ , expressed in synchronous watts referred to the synchronous speed  $\omega_s$ , is now given by the power dissipated in the rotor resistance,  $R_2 \omega_s / (\omega_1 - \omega_2)$ , so that,

$$T = I_2^2 R_2 \omega_s / (\omega_1 - \omega_2)$$

The circuit shown in Fig. 3 is valid for any values of  $\omega_1$  and  $\omega_2$ . Under dynamic-braking conditions the stator current is direct, i.e.  $\omega_1 = 0$ , and the equivalent stator applied voltage and resistance become infinite. This corresponds to the conditions assumed in the application of Thévenin's theorem for a constant current. When  $\omega_1 = 0$  the effective rotor resistance becomes  $-R_2 \omega_s / \omega_2$  and  $T = -I_2^2 R_2 \omega_s / \omega_2$ , which, being negative, represents a braking torque. In the usual dynamic-braking equivalent circuit the negative sign of the equivalent rotor resistance is omitted, which is quite in order so long as it is recognized that the torque is opposed in direction to the rotation.

The stator current  $I_1$  is the alternating current of angular frequency  $\omega_s$  which is equivalent to the actual direct stator

excitation current  $I_D$  (see Section 9.1). By specifying the latter and so also  $I_1$ , the stator impedance and voltage need not be shown and Fig. 3 can be further simplified to Fig. 4.

Fig. 4.—Dynamic-braking equivalent circuit.  
Frequency,  $\omega_s$ .

### (3.2) Torque Equation

To simplify the expressions, the equivalent rotor resistance,  $R_2 \omega_s / \omega_2$ , will be denoted by  $R$ . Vector quantities are shown in heavy type.

From the circuit diagram (Fig. 4)

$$I_1 = I_m + I_2 \quad (1)$$

$$I_m = V_2 / jX_m \quad (2)$$

$$I_2 = V_2 / (R + jX_2) \quad (3)$$

From eqns. (2) and (3)

$$I_m = I_2 (R + jX_2) / jX_m$$

Substituting in eqn. (1) gives

$$I_1 = I_2 (R + jX_2) / jX_m + I_2 \\ = I_2 [R + j(X_2 + X_m)] / jX_m$$

therefore

$$I_2 = \frac{I_1 jX_m}{R + j(X_2 + X_m)}$$

and

$$I_2^2 = \frac{I_1^2 X_m^2}{R^2 + (X_2 + X_m)^2}$$

$$\text{The torque, } T, = I_2^2 R = \frac{I_1^2 X_m^2 R}{R^2 + (X_2 + X_m)^2} \quad (4)$$

For unsaturated conditions  $X_m$  is constant, and the maximum value of  $T$  is found from eqn. (4) to occur when

$$R = (X_2 + X_m) \quad (5)$$

and

$$T_{\max} = \frac{I_1^2 X_m^2}{2(X_2 + X_m)} \quad (6)$$

Eqns. (5) and (6) show that the maximum torque for a given value of  $I_1$ , or of  $I_D$ , is independent of  $R$ , but that the speed,  $\omega_2$ , at which the maximum torque occurs varies directly as the rotor-circuit resistance  $R_2$ , since  $R = R_2 \omega_s / \omega_2$ . This relationship between resistance and speed is analogous to that between resistance and slip in normal induction-motor operation.

$(X_2 + X_m)$  is the synchronous reactance at the angular frequency  $\omega_s$ . Eqn. (4) can be used to find the torque even under saturated conditions if the value of  $X_m$  taken is the adjusted value as used in alternator calculations. However, this usually involves a tedious process of trial and error, the graphical construction given below being much less laborious.

Even in the presence of saturation, when  $X_m$  cannot be taken as constant, it remains true that for a given value of  $I_1$  the torque

is a function of  $R$  only, since for a particular value of  $R$ , the quantities  $I_2$ ,  $I_m$  and  $V_2$ , the degree of saturation and the value of  $X_m$  are all fixed. Thus if the relationship between the torque and  $R$  for any particular value of  $I_1$  is derived as shown in the following Section, the torque/speed curves for given values of  $R_2$  may be obtained from it, and also the torque/resistance curves for given speeds. Since  $R = R_2\omega_s/\omega_2$ , the speeds are obtained from

$$\omega_2 = R_2\omega_s/R \quad (7)$$

and the resistances for given speeds from

$$R_2 = \omega_2 R / \omega_s \quad (8)$$

### (3.3) Graphical Construction

The curve OC shown in Fig. 5 is the magnetization characteristic of the induction motor, i.e. the relation between the air-gap

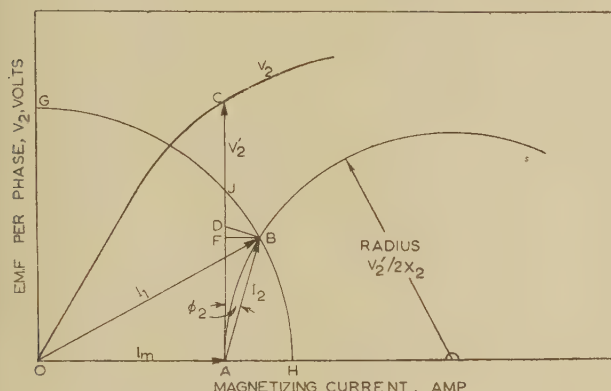


Fig. 5.—Graphical construction for the calculation of dynamic-braking characteristics.

e.m.f. per phase,  $V_2$ , and the magnetizing current per phase,  $I_m$ , for the unit or synchronous speed,  $\omega_s$ . This curve may be measured as described in the next Section. Also shown in Fig. 5 is the vector diagram OAB, for the circuit in Fig. 4, showing the currents  $I_1$ ,  $I_m$ ,  $I_2$ , for the particular e.m.f.  $V_2' = AC$ . This vector diagram corresponds to eqn. (1). For a given direct stator excitation current  $I_D$ ,  $I_1$  is also fixed in magnitude, so that the point B lies on the quadrant of circle GBH, radius  $I_1$ . Since the rotor circuit of Fig. 4 consists of a fixed reactor in series with a variable resistor, the extremity of the current vector  $I_2$  lies on the circle AB, of diameter  $V_2'/X_2$  or radius  $V_2'/2X_2$ . Hence the point B must be the intersection of the two circles, and for the particular value of e.m.f. chosen ( $V_2' = AC$ ), the vector diagram must be as shown.

The phase angle between  $V_2'$  and  $I_2 = \phi_2 = \text{angle CAB}$ , and if BF is drawn perpendicular to AC, the torque per phase, in synchronous watts, is given by

$$T = V_2' I_2 \cos \phi_2 = AC \times AF \quad (9)$$

(AC is measured in volts and AF in amperes.)

Also, if DB is drawn perpendicular to AB, to cut AC at D,  $AD = I_2 / \cos \phi_2$ , and since  $I_2 = V_2' / \sqrt{R^2 + X_2^2}$ , and  $\cos \phi_2 = R / \sqrt{R^2 + X_2^2}$ , it follows that, if AD is measured in amperes,

$$AD = V_2' / R$$

and therefore

$$R = V_2' / AD \quad (10)$$

By repeating this construction for a number of values of  $V_2'$ , the relationship between  $T$  and  $R$  may be determined, from which the torque/speed curves for given values of  $R_2$ , or the

torque/resistance curves for given speeds, may be derived, as explained above. This procedure can be carried out for the values of  $I_1$  corresponding to any values of  $I_D$ .

If  $X_2$  is very small, the circle AB becomes very large, the angle  $\phi_2$  becomes very small, and the points D, F and B all tend to coincide with J. In many cases it may be sufficiently accurate to assume that  $X_2$  is negligible, so that the torque is given by

$$T = AJ \text{ (in amperes)} \times AC \text{ (in volts)} \quad (11)$$

and the equivalent rotor resistance by

$$R = \frac{AC \text{ (in volts)}}{AJ \text{ (in amperes)}} \quad (12)$$

It should be noted that  $X_2$  is the leakage reactance of the rotor alone, and not the total leakage reactance which is used in the usual approximate equivalent circuit of the induction motor.

### (4) MEASUREMENT OF THE MOTOR PARAMETERS

When a machine is still in the design stage, the parameters must be calculated from the design data. Cochran<sup>7</sup> has suggested a method of determining the relation between the air-gap e.m.f. and magnetizing current. The rotor leakage reactance and resistance can be calculated by any of the well-known methods used for induction motors.<sup>6,8</sup>

With an existing machine simple tests suffice to give the required information.

#### (4.1) Slip-Ring Motor

The obvious and probably the most accurate way of determining the magnetization characteristic of a slip-ring motor is to drive it at a constant speed, measuring the open-circuit voltages between the slip-rings for a complete range of d.c. stator excitations. The direct stator currents,  $I_D$ , are then converted to the equivalent alternating currents,  $I_1$ , referred to the rotor, as shown in Section 9.1. With a slip-ring motor it is often simplest to refer all quantities to the rotor, particularly if external rotor resistors are being used. Otherwise the measured slip-ring voltages are referred to the stator, together with the equivalent alternating currents  $I_1$ .

In any case, the transformation ratio of the machine is required. This is measured by a static test with the rotor open-circuited, the stator being supplied from a balanced 3-phase source. It is necessary to correct for the voltage drop in the stator leakage reactance if accuracy is to be obtained. If this voltage drop is neglected in the motor used for the tests described later, the ratio error is about 5%. It is not usually necessary to allow for the stator resistance, since the magnetizing current is nearly in quadrature with the voltage. Since there may be some difficulty in measuring the leakage reactances accurately, it is preferable to measure the ratio also by supplying the rotor and measuring the open-circuit stator voltage. The mean of the two values obtained should be quite accurate enough.

The stator and rotor leakage reactances can be measured only by means of a locked-rotor test. The reactance so measured is effectively the total leakage reactance referred to the stator, and it is usually considered accurate enough to attribute half to the stator and half to the rotor. If the locked-rotor test is repeated by short-circuiting the stator and supplying the rotor, a good check of the transformation ratio can be obtained. If  $X_S$  is the total reactance measured from the stator side and  $X_R$  is that measured from the rotor side, the stator/rotor ratio  $= \sqrt{(X_S/X_R)}$ .

The stator and rotor resistances may be measured by any suitable method and the results checked against the total resistance obtained from the locked-rotor test.

## (4.2) Squirrel-Cage Motor

It is possible to make measurements only from the stator side of a squirrel-cage motor, and it is therefore more difficult to obtain such accurate results as those for a slip-ring motor. The magnetization curve cannot be determined in the manner described in the previous Section, but a reasonably accurate result can be obtained by running the motor light from a.c. mains of known frequency, adjusting the voltage over the widest possible range and measuring the input current and power-factor for each voltage. The e.m.f.'s are then calculated by allowing for the stator reactance voltage-drop. Again, it is not usually necessary to allow for the stator resistance, but this is easily done if required. Since the current is very nearly in quadrature with the voltage, the e.m.f.,  $V_2$ , is obtained from the supply voltage measured,  $V$ , by eqn. (13):

$$V_2 = V - IX_1 \quad \dots \quad (13)$$

where  $I$  is the measured current and  $X_1$  is the stator leakage reactance.

It will be realized that the current,  $I$ , as measured in this way, is not the true magnetizing current, since there must be some rotor current to provide the driving torque. More accurate results can be obtained by driving the induction motor under test by a d.c. motor at synchronous speed. This can be found, as nearly as required for this purpose, by adjusting the speed until the input current to the induction motor is a minimum.

To check the accuracy obtainable by the method described above, tests were carried out on a slip-ring motor, so that the results could be compared with those determined by the d.c.-excitation method described in Section 4.1. The induction motor was coupled to a d.c. machine so that it had some extra load above that of the motor itself. The slip-rings were short-circuited and the machine was supplied from the 50 c/s mains through a tapped transformer, at various voltages. The currents and powers were measured and the e.m.f.'s calculated in accordance with eqn. (13). It was found that the stator resistance drop was negligible, but that the current for a given e.m.f. was about 3% greater than the true magnetizing current. When the d.c. machine was also supplied and the speed adjusted to the synchronous speed, as suggested above, no significant difference could be noted between the results and those obtained by the method of Section 4.1.

A locked-rotor test at reduced voltage gives the total resistance and reactance referred to the stator. The stator resistance may be measured by any suitable method, and the rotor resistance referred to the stator then calculated. The value so obtained will be that corresponding to the frequency used in the test, and it is desirable to repeat the test at a number of other frequencies to determine how the resistance varies with rotor frequency. The rotor reactance may be taken as half the total measured reactance without causing any appreciable loss of accuracy in calculating the torque/speed curves. Fortunately, errors tend to cancel to some extent, since, if the rotor reactance is taken as higher than the proper value and the stator reactance underestimated, the calculated values of e.m.f. will be too high. Too high a rotor reactance  $X_2$  will reduce the calculated torques, but too high an e.m.f. will increase them. In any case, the rotor reactance does not have a great effect, as an examination of the curves given in Figs. 10 and 11 will show.

## (5) TEST RESULTS

To confirm the accuracy of the graphical construction described in Section 3.3 for predicting the dynamic-braking torques of induction motors, a number of tests were carried out on a 15 h.p. 3-phase 400-volt 50 c/s 6-pole slip-ring motor

of normal industrial design. This machine was coupled to a d.c. machine which was used as a separately excited motor, supplied from a motor-generator on the Ward Leonard system. The field current of the d.c. machine was maintained constant throughout all the tests, so that the loss torques could be calculated as accurately as possible.

The loss torque comprises

- The torque of the d.c. machine, which was taken to depend on the speed only, the field current being constant.
- The friction and windage torque of the induction motor, depending on the speed.
- The torque due to the iron loss in the induction-motor rotor, which was taken to depend on the speed and on the air-gap e.m.f.

Tests for calibration of the machine set were carried out by supplying the d.c. motor from the Ward Leonard set, the induction motor slip-rings being open-circuited except for a voltmeter to measure the e.m.f. The set was run at a number of speeds, and for each speed the d.c. motor armature input powers were measured for the full range of direct excitation currents of the induction motor stator. Curves were then plotted of loss torque against air-gap e.m.f. for each of the test speeds. The loss torques were expressed in synchronous watts at 800 r.p.m. (40 c/s), since the magnetization characteristic of the induction motor was measured at this speed. It is not necessary to take account of the voltage drop in the armature of the d.c. machine, as this does not affect the torque. Typical examples of the working out of the results will be found in Section 9.2.

Dynamic-braking tests were carried out over the widest possible range of speeds, the total braking torque being measured by the input to the d.c. driving motor. For each measurement the air-gap e.m.f. was calculated and the loss torque estimated from the calibration curves. The total input torque minus the loss torque then gave the net dynamic-braking torque which could be compared with the predicted torque.

## (5.1) Tests with External Rotor Resistance and Reactance

One of the objects of the tests being to establish the validity of the method of prediction described in Section 3.3, it was considered desirable to increase the reactance of the rotor circuit of the induction motor, so that its effect would be by no means negligible. This was done by connecting the slip-rings to three identical reactors (with iron/air cores), so increasing the reactance to about four times that of the motor alone.

The results obtained for a number of speeds and stator excitations, with external rotor resistances, are shown in Figs. 6,

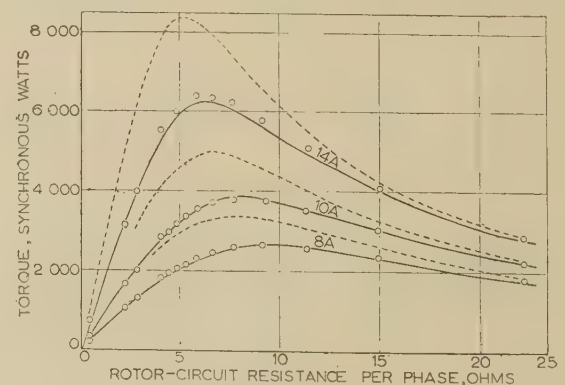


Fig. 6.—Torque/resistance characteristics.

Speed: 800 r.p.m.  
 Direct stator currents as shown.  
 — Predicted allowing for  $X_2$ .  
 - - - Predicted neglecting  $X_2$ .  
 ○ ○ Measured.

7, 8, and 9. These show the predicted torques allowing for the rotor-circuit reactance ( $X_2$ ), the predicted torques neglecting the rotor-circuit reactance, and the measured torques. The high

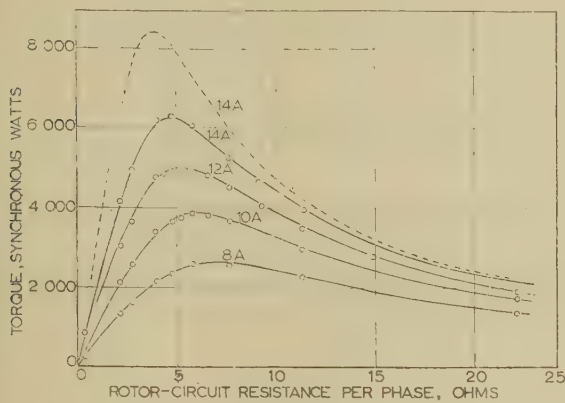


Fig. 7.—Torque/resistance characteristics.

Speed: 600 r.p.m.  
Direct stator currents as shown.  
— Predicted allowing for  $X_2$ .  
--- Predicted neglecting  $X_2$ .  
○ ○ Measured.

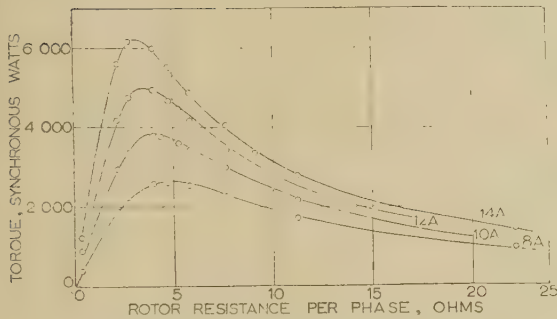


Fig. 8.—Torque/resistance characteristics.

Speed: 400 r.p.m.  
Direct stator currents as shown.  
— Predicted.  
○ ○ Measured.

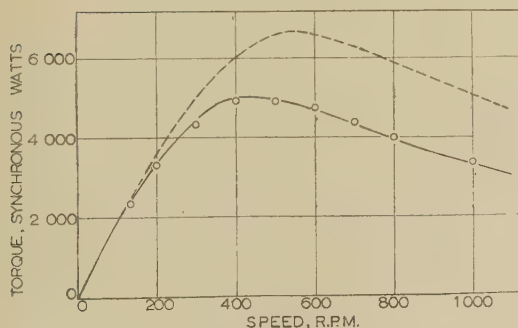


Fig. 9.—Torque/speed characteristic.

$R_2 = 4$  ohms.  
 $X_2(40 \text{ c/s}) = 2.3$  ohms.  
Direct stator current = 12 amp.  
— Predicted allowing for  $X_2$ .  
--- Predicted neglecting  $X_2$ .  
○ ○ Measured.

## (5.2) Tests with Rotor Short-Circuited

Tests were also performed over the speed range 0–1000 r.p.m. with the slip-rings of the induction motor short-circuited, the results obtained, together with those predicted, being shown in Figs. 10 and 11. Tests were also carried out with other values of excitation with similar results to those shown.

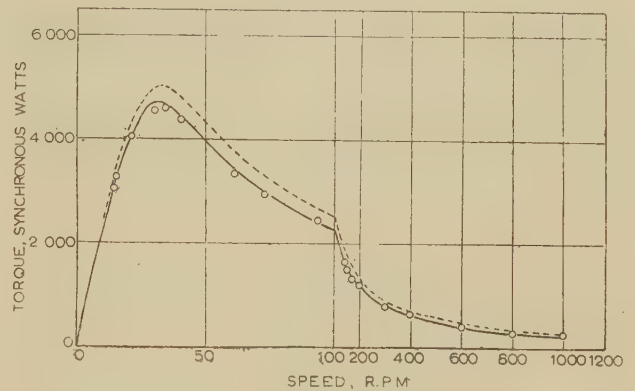


Fig. 10.—Torque/speed characteristic.

Rotor short-circuited.  
 $R_2 = 0.27$  ohm.  
 $X_2(40 \text{ c/s}) = 0.45$  ohm.  
Direct stator current = 10 amp.  
— Predicted allowing for  $X_2$ .  
--- Predicted neglecting  $X_2$ .  
○ ○ Measured.  
Note change of speed scale.

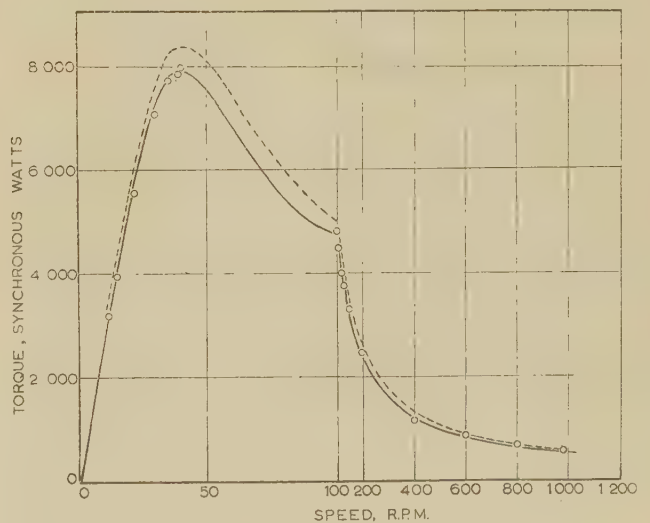


Fig. 11.—Torque/speed characteristic.

Rotor short-circuited.  
Direct stator current = 14 amp.  
— Predicted allowing for  $X_2$ .  
--- Predicted neglecting  $X_2$ .  
○ ○ Measured.  
Note change of speed scale.

order of accuracy obtainable by using the method of prediction given in Section 3.3 is evident from these curves, but it is also evident that the rotor reactance cannot be neglected.

Again, the predicted torques allowing for  $X_2$  are more accurate than those obtained if  $X_2$  is neglected, particularly around the peak torque. However, as shown later, for calculating the total running-down time of a motor with short-circuited rotor, the most important part of the torque/speed curve is that where the torque is low—at the higher speeds. The peak torque is of very minor importance in this connection.

In Figs. 6-11 the torques are expressed in 3-phase synchronous watts, based again on the synchronous speed 800 r.p.m. The magnetization characteristic was measured at this speed, so that the predicted torques appear directly in these units.

### (6) CALCULATION OF RUNNING-DOWN TIME

#### (6.1) Loss Torques

The curves in Figs. 6-11 show the net dynamic-braking torques, but the total torques including those due to losses, are required for calculating the time taken for a machine to decelerate from full-speed to standstill. In a slip-ring motor with short-circuited rotor, or a squirrel-cage motor, the loss torque may actually be greater than the dynamic-braking torque at high speeds. For the slip-ring motor set used by the author the torques at 1000 r.p.m. were as follows:

- $I_D = 8$  amp:  
 Net dynamic braking torque = 165 synchronous watts  
 Loss torque = 360 synchronous watts
- $I_D = 14$  amp:  
 Net dynamic braking torque = 585 synchronous watts  
 Loss torque = 365 synchronous watts

Much greater loss torques than these would be experienced in many industrial drives, so that it is necessary to measure them as accurately as possible.

With a slip-ring motor coupled to a d.c. machine the measurement of the loss torque is quite straightforward, since the machine set can be calibrated in the manner described in Section 5, the loss torques being related to the speed and to the motor e.m.f. In using the construction of Fig. 5 to predict the dynamic-braking torque, the e.m.f. at the synchronous speed is known, and the corresponding speed is calculated. The actual e.m.f. can therefore be calculated and the loss torque estimated.

In a squirrel-cage motor it is not possible to open the rotor circuit, and the total losses including the rotor iron loss cannot be measured. Possibly the best practical compromise is to assume that the stator iron loss, for a given flux and frequency is equal approximately to the rotor iron loss for the same flux and frequency. The loss measurements can then be made by supplying the motor at various voltages and frequencies, measuring the input powers, currents and voltages. It is then possible to calculate the air-gap e.m.f.'s and the sum of the mechanical losses and stator iron losses. Such tests could conveniently be combined with those carried out to determine the magnetization characteristic, described in Section 4.2.

These difficulties with squirrel-cage motors, together with the desirability of being able to increase the rotor reactance, led to the choice of a slip-ring motor for the tests described herein.

#### (6.2) Running-Down Time

The average braking torque is commonly taken as the basis of comparison of torque/speed curves and used for calculating the total time of deceleration.<sup>2</sup> This is incorrect and may lead to very serious errors, which can be shown as follows:

If  $T$  is the total decelerating torque at the speed  $N$ ,

$$-\frac{dN}{dt} = kT \quad (14)$$

where  $k$  is a constant depending on the inertia.

Rearranging eqn. (14) gives:

$$dt = -\frac{dN}{kT}$$

If  $t = t_1$  at  $N = N_1$ , and  $t = t_2$  at  $N = N_2$ ,

$$\int_{t_1}^{t_2} dt = - \int_{N_1}^{N_2} \frac{dN}{kT} = + \int_{N_2}^{N_1} \frac{dN}{kT}$$

Thus the time taken to decelerate from  $N_1$  to  $N_2$  is

$$t_2 - t_1 = \int_{N_2}^{N_1} \frac{dN}{kT} \quad (15)$$

The right-hand side of eqn. (15) is the area under the curve of  $1/kT$  plotted as ordinate against  $N$  as abscissa, between  $N_2$  and  $N_1$ . The effective mean braking torque is therefore the inverse of the mean of  $1/T$ , which may be very different from the simple mean value of  $T$ , particularly for torque/speed curves of the shape obtained with dynamic braking.

#### (6.2.1) Motor with Short-Circuited Rotor.

For a slip-ring motor with short-circuited rotor, or for a squirrel-cage motor, the dynamic braking characteristics are of the form shown in Figs. 10 and 11. For the conditions as in Fig. 10, the inverse of the total torque (including loss) is shown plotted against speed in Fig. 12, curve (A). As explained above,

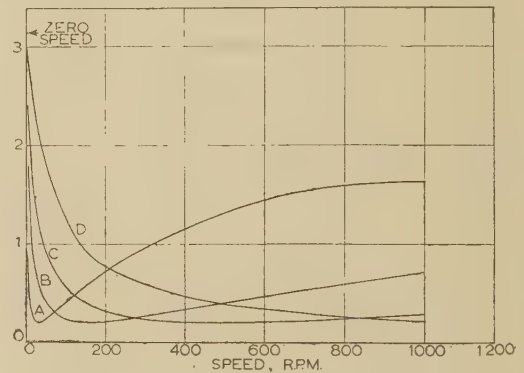


Fig. 12.—Inverse of torque plotted against speed.

Direct stator current = 10 amp.  
 Ordinate scale =  $1000/(\text{torque in synchronous watts})$ .  
 (A)  $R_2 = 0.27$  ohm (rotor short-circuited).  
 (B)  $R_2 = 1.35$  ohms.  
 (C)  $R_2 = 4.05$  ohms.  
 (D)  $R_2 = 13.5$  ohms.

the area under this curve between any two speeds is a measure of the time to decelerate from the upper to the lower speed. The total time from 1000 r.p.m. to standstill is 1.2 units, where one unit is equal to the ratio (revolutions per minute)/(synchronous watts). If the torques corresponding to curve (A) of Fig. 12 are plotted and the simple average taken, the apparent decelerating time worked out from this is 0.87 unit. This is an error of 27%. For higher direct excitation currents the error is even greater. For example, for  $I_D = 14$  amp the correct decelerating time is 0.72 unit, whereas the value obtained by using the average torque is 0.3 unit, which is an error of over 50%.

It is quite obvious from curve (A) of Fig. 12 that the most significant part of the curve is that where  $1/T$  is high, i.e. where  $T$  is small, the value of the peak torque having little effect on the total running-down time. This may perhaps be illustrated by supposing that it were possible to double all the torques from zero speed to 200 r.p.m., leaving the torques from 200 to 1000 r.p.m. unchanged. The consequent reduction in the total time would be only about 4%.

If the dynamic-braking torques are calculated by the approxi-

mate method, neglecting the rotor leakage reactance, the calculated running-down time is less than the correct value by about 10%. This may be sufficiently accurate in many cases.

#### (6.2.2) Slip-Ring Motor with External Resistor.

Quicker deceleration may be obtained with a slip-ring motor by increasing the rotor-circuit resistance. Ideally, the rotor resistance should be continuously adjusted as the speed falls, to maintain the braking torque at the maximum. This is impracticable for industrial drives, although it has been used with mine winders. However, it is quite feasible to arrange the control system of the motor so as to insert part of the starter resistance in the rotor circuit during the braking period. This can reduce considerably the time required to bring the machine to rest. Curves (b), (c) and (d) in Fig. 12 show the effect of increasing the rotor resistance of the motor used by the author. By drawing a number of such curves and plotting the total running-down time against the rotor-circuit resistance, the optimum value of the latter was found to be that corresponding to curve (c). By using this resistance the total time is reduced to 25% of that obtained with the rotor winding short-circuited.

### (7) CONCLUSION AND ACKNOWLEDGMENTS

The results of all the tests carried out confirm the accuracy of the graphical construction used for predicting the dynamic braking torques. Although not shown by the results as presented, it was very evident from the working out of these that the effects of saturation cannot be ignored without serious error. The errors due to neglecting the leakage reactance of the rotor are not so great.

The author wishes to acknowledge the help received in discussions with Mr. O. I. Butler, Dr. T. H. Barton and Dr. B. C. Doxey. The experimental work was carried out in the laboratories of the Department of Electrical Engineering of the University of Sheffield.

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### (9) APPENDICES

#### (9.1) Relation between $I_D$ and $I_1$

The relationship between the direct stator excitation current,  $I_D$ , and the equivalent r.m.s. alternating current,  $I_1$ , depends on the stator connection used. The commonly used schemes of connection are shown in Figs. 13(a) and 13(b) for star-connected machines and in 13(c) and 13(d) for delta-connected machines. These all require the same total power to produce the same effective excitation m.m.f. Scheme (a) gives more uniform heating than scheme (b), but the former requires a 3-pole braking

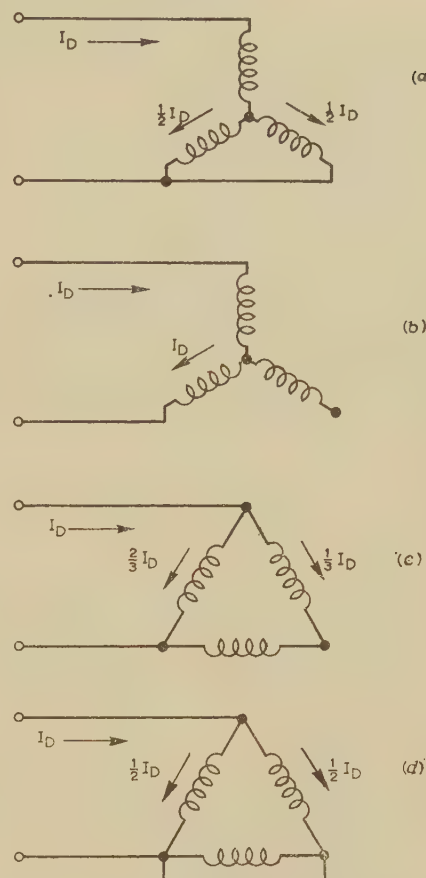


Fig. 13.—Dynamic-braking stator connections.

contactor, whereas the latter only requires a 2-pole one. Where the braking period is only a small part of the operation cycle, as in most industrial applications, it is unlikely that the heat generated will constitute a serious problem and the simpler method (b) will be preferred. For the large slip-ring motors used for mine winders, where braking is very frequent, connection (a) seems to be preferred in general. For delta-connected machines, (c) gives more uniform heating than (d), and being simpler, it is to be preferred.

In connection (a) the direct currents flowing in the windings correspond to the equivalent alternating currents "frozen" at the instant when the current in phase A has its peak value. Therefore,  $I_D = \sqrt{2}I_1$ ,  $I_1$  being the r.m.s. current. Thus,  $I_1$ , referred to the stator, is  $I_D/\sqrt{2}$ .

For connection (b) the conditions correspond to the instant when the alternating current in phase C is zero and that in A is  $\sqrt{3}/2$  times the peak value. Therefore,  $I_D = \sqrt{3}I_1/\sqrt{2}$ , or,  $I_1 = \sqrt{2}I_D/\sqrt{3}$ .

Similar arguments are used to find the relationships for connections (c) and (d). In the former, if  $I_1$  is the r.m.s. phase current,  $2I/3 = \sqrt{2}I_1$ , or  $I_1 = \sqrt{2}I_D/3$ . In the latter,  $I_1 = I_D/\sqrt{6}$ .

If all quantities are to be referred to the rotor, which is usually more convenient for a slip-ring motor, the values of  $I_1$  obtained as above must be multiplied by the transformation ratio of the machine.

#### (9.2) Typical Calculations

##### (9.2.1) Prediction of Torque/Resistance characteristic, with External Rotor Reactance.

The magnetization curve used in the construction of Fig. 5 is drawn for 800 r.p.m. and the torques are expressed in synchronous

watts at this speed. The stator connection used was that shown in Fig. 13(a).

The rotor-circuit reactance per phase at 40 c/s is 2.3 ohms, and the stator/rotor turns ratio is 2.88 : 1.

For  $I = 14$  amp,  
 $I_1$  (referred to rotor) =  $14 \times 2.88/\sqrt{2} = 28.5$  amp.

This is the radius of the  $I_1$  circle (GJBH).

For  $V'_2 = 100$  volts,  $I_m = 11.9$  amp.

The radius of the circle  $AB = 100/4.6 \approx 21.75$  amp. Therefore the centre of this circle is at  $11.9 + 21.75 = 33.65$  amp. If the circle is drawn to pass through A, it intersects the  $I_1$  circle at B, and

$$AB = I_2 = 20.8 \text{ amp}$$

$$AF = I_2 \cos \phi_2 = 18.3 \text{ amp}$$

and  $AD = I_2/\cos \phi_2 = 23.6$  amp.

From eqn. (9),

$$T = 18.3 \times 100 = 1830 \text{ synchronous watts}$$

$$\text{Total torque} = 3 \times 1830 = 5490 \text{ synchronous watts}$$

From eqn. (10),

$$R = 100/23.6 = 4.24 \text{ ohms.}$$

This net dynamic-braking torque is therefore obtained with the following values of  $R_2$ , total rotor-circuit resistance per phase:

$$800 \text{ r.p.m., } R_2 = 4.24 \text{ ohms}$$

$$600 \text{ r.p.m., } R_2 = 4.24 \times \frac{3}{4} = 3.18 \text{ ohms}$$

$$400 \text{ r.p.m., } R_2 = 4.24 \times \frac{1}{2} = 2.12 \text{ ohms}$$

If the rotor reactance is neglected,  $I_2 = AJ = 25.8$  amp.

$$T = 100 \times 25.8 = 2580 \text{ synchronous watts}$$

$$\text{Total torque} = 7740 \text{ synchronous watts}$$

$$R = 100/25.8 = 3.73 \text{ ohms } (= R_2 \text{ for } 800 \text{ r.p.m.})$$

#### (9.2.2) Prediction of Torque/Speed Characteristic, with External Rotor Reactance.

The conditions are as above, except that  $I_D = 12$  amp, giving  $I_1$  (referred to rotor) = 24.4 amp.

For  $V'_2 = 80$  volts,  $I_m = 9.2$  amp, and the radius of the circle  $AB = 80/4.6 = 17.4$  amp. When this circle is drawn,

$$AB = I_2 = 18.2 \text{ amp}$$

$$AF = I_2 \cos \phi_2 = 15.5 \text{ amp}$$

and  $AD = I_2/\cos \phi_2 = 21.4$  amp

$$T = 80 \times 15.5 = 1240 \text{ synchronous watts}$$

$$(3T = 3720 \text{ synchronous watts})$$

$$R = 80/21.4 = 3.74 \text{ ohms.}$$

For the curve shown in Fig. 9,  $R_2 = 4.0$  ohms, and therefore the above calculation corresponds to the speed

$$N = 4.0 \times 800/3.74 = 855 \text{ r.p.m.}$$

Neglecting  $X_2$ ,  $AJ = I_2 = 22.6$  amp, so that

$$T = 22.6 \times 80 = 1808 \text{ synchronous watts}$$

$$(3T = 5424 \text{ synchronous watts})$$

$$R = 80/22.6 = 3.54 \text{ ohm}$$

$$N = 4.0 \times 800/3.54 = 903 \text{ r.p.m.}$$

#### (9.2.3) Calculation of Net Braking Torque from Test Results.

##### (a) The e.m.f. of the d.c. motor.

When running light at 800 r.p.m. with a field current of 1.0 amp, the armature voltage is 180 volts, and the current 2.9 amp. The armature-resistance voltage drop is  $0.35 \times 2.9 = 1$  volt, and taking the brush drop as 1 volt, the e.m.f. = 178 volts. This checks with tests at several other speeds, and also with tests made with the induction motor driving the d.c. machine, the latter separately excited with a field current of 1.0 amp.

Therefore, the total input torque, measured in synchronous watts at 800 r.p.m., is 178 times the armature current in amperes. With the field current maintained at 1.0 amp, this applies whatever the speed, since the e.m.f. is directly proportional to the speed.

##### (b) Braking test with external rotor impedance.

$$\text{Speed} = 800 \text{ r.p.m. } I_D = 14 \text{ amp}$$

$$R_2 \text{ (total)} = 2.8 \text{ ohms per phase}$$

$$\text{Rotor winding resistance} = 0.26 \text{ ohm per phase}$$

$$X_2 \text{ (total)} = 2.3 \text{ ohms per phase}$$

$$\text{Rotor leakage reactance} = 0.45 \text{ ohm per phase.}$$

$$\text{D.C. motor armature current} = 24.6 \text{ amp}$$

$$\text{Total input torque} = 178 \times 24.6 = 4380 \text{ synchronous watts.}$$

With external rotor impedance whose power, voltage and current can be measured, it is possible to estimate the air-gap e.m.f. as follows:

$$\text{Slip-ring voltage} = 198$$

$$\text{Voltage per phase} = 66 \text{ volts}$$

$$\text{Current} = 21.9 \text{ amp}$$

$$3\text{-phase power} = 3500 \text{ watts.}$$

The power factor is therefore 0.805, and the voltage drop in the rotor winding, taken as  $RI \cos \phi + XI \sin \phi$ , is 12.4 volts per phase. Hence the air-gap e.m.f. is 78.4 volts per phase. From the loss-torque curves for the speed 800 r.p.m., the loss torque is 390 synchronous watts. Therefore, the net dynamic-braking torque is  $(4380 - 390) = 3990$  synchronous watts.

This figure can also be checked in the following way: The loss in the external rotor impedance, measured by the wattmeter, is 3500 watts, and the resistance loss in the rotor winding is  $3 \times 0.26 \times 21.9^2 = 375$  watts; the total loss is therefore 3875 watts. This is less than the previous figure by 2.9%, which is quite satisfactory, since the wattmeter used—although sufficiently accurate for estimating the minor drop in the rotor winding—was hardly more accurate than the error.

#### (9.2.4) Prediction of Torque/Speed Characteristic with Rotor Short-Circuited.

$$I_D = 14 \text{ amp}$$

$$I_1 = 28.5 \text{ amp}$$

$$X_2 = 0.45 \text{ ohm (40 c/s)}$$

$$R_2 = 0.27 \text{ ohm}$$

For  $V'_2 = 120$  volts,  $I_m = 16.2$  amp and the radius of the  $I_2$  circle =  $120/0.9 = 133.3$  amp.

from the construction,  $I_2 = 21.9$  amp,  $I_2 \cos \phi = 21.9$  amp, and  $I_2/\cos \phi_2 = 22.0$  amp  
 the total net dynamic braking torque  $= 3T = 3 \times 120 \times 21.9 = 7880$  synchronous watts.

$$R = 120/22 = 5.45 \text{ ohm.}$$

$$\text{Speed } N = 800 \times 0.27/5.45 = 39.7 \text{ r.p.m.}$$

If  $X_2$  is neglected,  $I_2 = 23.3$  amp, giving  
 net torque  $= 8380$  synchronous watts.  
 $R = 5.15$  ohms.  
 $N = 42$  r.p.m.

In the above example it is noteworthy how great is the influence of the rotor reactance, in spite of the fact that the frequency is only about 2c/s. The error in both torque and speed if this reactance is neglected is about 6%.

### DISCUSSION BEFORE THE UTILIZATION SECTION, 17TH NOVEMBER, 1955

**Prof. G. H. Rawcliffe:** The results which the author has obtained from the equivalent circuit, for effective resistance and for torque, can be much more readily obtained from energy considerations:

$$\begin{aligned} (\text{braking torque}) \times (\text{angular velocity}) &= (\text{braking power}) \\ &= (\text{energy dissipated in rotor}) \end{aligned}$$

Therefore  $T \times 2\pi\omega_2 = I^2 R_2$   
 $T \times 2\pi\omega_s = I^2 R_2 (\omega_s/\omega_2)$

Therefore, torque (in synchronous watts)  $= I^2 R_2 (\omega_s/\omega_2)$   
 and effective resistance  $= R_2 (\omega_s/\omega_2)$

The torque equation follows at once from the two facts that the effective magnetizing current is, as always, the vector difference between the (equivalent) stator current and the rotor current; and that the generated rotor voltage, which circulates the rotor current through the rotor impedance, is also the voltage acting across the magnetizing impedance. Physically, it is clear that the special feature of an induction motor in the d.c. dynamic braking regime is that the flux is variable in the first order, whereas in normal induction-motor action the flux is nearly constant. The torque equation expresses this physical fact mathematically. It is because the flux, whilst braking dynamically, is so much reduced that the effect of ignoring leakage reactance is so much smaller than might have been expected.

I do wonder whether the influence of the great John Hopkinson has not even been too powerful and prolonged. In 1886 it was natural for him to obtain magnetizing characteristics by driving machines as generators: in 1955 I think one should be content with the simpler processes involved in obtaining the characteristics while motoring, if necessary making the very small correction which arises as between motoring and generating regimes, and I doubt therefore whether the author's open-circuit tests are really warranted.

In conclusion, it might be added that there is a strong similarity between the d.c./a.c. relationships in the author's first appendix and those which exist in a synchronous induction motor, as was discussed by myself at length in an earlier Institution paper.\*

**Mr. O. I. Butler:** The author has shown, for the first time, that it is possible to predict accurately the d.c. dynamic braking torque/speed characteristic of an induction motor when the

### (9.2.5) Calculation of Net Braking Torque from Test Results with Rotor Short-Circuited.

$$\text{Speed} = 38 \text{ r.p.m.}$$

$$I_D = 14 \text{ amp.}$$

$$\text{Measured d.c. motor armature current} = 45.2 \text{ amp.}$$

$$\text{Total input torque} = 1678 \times 45.2 = 8050 \text{ synchronous watts.}$$

$$\text{Measured value of } I_2 = 20.5 \text{ amp.}$$

$$\text{Frequency} = 1.9 \text{ c/s.}$$

$$\text{Actual rotor reactance} = 0.023 \text{ ohm.}$$

$$\text{Effective impedance} = R_2 = 0.27 \text{ ohm.}$$

$$\text{Actual air-gap e.m.f.} = 0.27 \times 20.5 = 5.55 \text{ volts.}$$

From the loss torque curves, loss torque  $= 190$  synchronous watts. Therefore the net dynamic braking torque is  $(8050 - 190) = 7860$  synchronous watts.

braking torques due to the iron and stray losses, and friction and windage, are not included in the characteristic. That is, the simple equivalent circuit of Fig. 4 is valid for the hypothetical condition of negligible iron and stray losses of the motor during braking. Is the author able to indicate what proportion of the loss torque, discussed in Sections 5 and 6.1, can be attributed to the induction motor alone?

In comparing the merits of a.c. and d.c. dynamic braking arrangements, I have found it desirable to evolve a mathematical method of analysis (awaiting publication) which includes the effect of saturation in the d.c. case. In evolving the method, it has been necessary to depart from the established procedure, followed by the author, in using the quantity  $R = R_2 \omega_s/\omega_2$  as the independent variable of functions expressing the values of  $I_2$  and  $T$ . Instead, I have used the magnetizing current,  $I_m$ , as the independent variable and shown that

$$I_2^2 = (I_1^2 - I_m^2)/(1 + 2X_2/X_m) \quad \text{. . . (A)}$$

$$T = I_2^2 \sqrt{(V_2^2/I_2^2 - X_2^2)} \quad \text{. . . (B)}$$

and  $R_2 N_s/N = \sqrt{(V_2^2/I_2^2 - X_2^2)} \quad \text{. . . (C)}$

The symbols are those used by the author, except for  $N_s$ , which represents the arbitrary synchronous speed of the machine. Obviously,  $X_m$  and  $V_2$  are functions of  $I_m$ , which can be obtained from the open-circuit curve of the machine.

A completely mathematical solution is obtained when the open-circuit curve is approximated by a sequence of straight lines or other convenient mathematical function of  $I_m$ . Alternatively, the open-circuit curve itself may be used direct. In either case, and for any degree of saturation, the conditions for maximum torque can readily be derived and evaluated. Thus, for instance, the variation of the maximum torque with  $I_1$ , and the speed at which maximum torque occurs, can be calculated directly and simply. Also, the torque/resistance and torque/speed curves can be readily calculated. In particular, using the mathematical solution, it is possible to demonstrate more conclusively that certain methods of a.c. dynamic braking are directly competitive with, if not an improvement upon, the d.c. dynamic braking method.

**Mr. E. W. Krebs:** In contrast to Prof. Rawcliffe, I think there is a good case for applying an equivalent circuit, provided that this approach leads to the same parameters as for the induction motor on a.c. operation, i.e. the short-circuit or locked-rotor impedance or reactance and the open-circuit or synchronous reactance. As the author has specifically pointed out, it is not

\* RAWCLIFFE, G. H.: "The Secondary Circuits of Synchronous Induction Motors," *Journal I.E.E.*, 1940, 87, p. 282.

the short-circuit reactance but the rotor leakage reactance alone that is required for the graphical construction of Fig. 5.

The reason for that is to be found in simplifying the equivalent circuit too far. It is not necessary, and perhaps not even desirable, to go over from Fig. 3 to Fig. 4 simply because the stator leakage reactance is eliminated. That it is possible to get the same results by using the short-circuit and no-load reactance can be shown by the following consideration. If an induction motor is excited with d.c., the torque performance is identical with that for a.c. excitation provided the losses in the stator winding and the frequency in the rotor are the same. Consequently it must be possible to arrive at the correct torque/speed relationship by the same means as for the induction motor.

The two apparently opposite views can easily be reconciled. Eqn. (4) gives the torque as a function of the impedances and the excitation. It is necessary only to reintroduce in that formula above and below the bar, both the stator resistance and the synchronous stator reactance at rated frequency. If that is done we find that the torque is proportional to the d.c. input multiplied by the difference of the no-load and short-circuit reactances divided by the stator resistance, and the rest of it is merely the reciprocal of  $[R/(X_2 + X_m) + (X_2 + X_m)/R]$ , as in the case of an induction motor.

The results are identical because nothing has actually been changed, but the modified method does no longer need the rather indefinable rotor leakage reactance or the separate treatment (and current conversion) for the various connections shown in Fig. 13. The latter is due to introducing, in place of the primary current, the primary power which, as is well known, is the same at any instant for any balanced polyphase system.

Saturation must undoubtedly be allowed for in the calculation of the torque because of the wide variation of flux, since (without external resistance in the rotor circuit) the magnetizing current varies almost in the ratio of the short-circuit to the no-load reactance. The method proposed by the author can be used with slight modifications to the terms for the voltage and the radius of the circle. This graphical method is very suitable for teaching purposes and illustration, but most designers would probably prefer an analytic method in tabulated form. The analytical method brings home the exact point made by a previous speaker that everything can be expressed as a function of the magnetizing current  $I_m$ , and if this is done and  $I_m$  is used as a starting-point, no process of trial and error is involved.

**Dr. J. E. Brown:** Fig. 3 of the paper is an equivalent circuit for balanced positive-sequence operation of the motor, where  $\omega_1$  is any frequency. The connections shown in Fig. 13 are all asymmetrical, and the arguments which lead to Fig. 4 are valid only for the special case of  $\omega_1 = 0$ .

When  $\omega_1 \neq 0$  the connections of Fig. 13 may be analysed by the symmetrical component method, and in fact part of the analysis for each connection has already been given in an earlier paper.\* Thus, taking either Fig. 13(a) or Fig. 13(d) as an example, it can be shown that the connection gives rise to equal positive- and negative-sequence voltages; it follows that two equivalent circuits are required to represent the performance completely. Further, it can be shown† that by a suitable choice

of rotor resistance the motor can be made to develop a braking torque for any value of  $\omega_2$ , the torque reducing to zero when  $\omega_2 = 0$ . The system has long been established on the Continent for dynamic braking and has the advantage over 'plugging' that there is no tendency to run-up in the reverse direction.

This method has an obvious disadvantage in that the positive-sequence system produces a driving torque when  $\omega_1 > \omega_2$ , a condition which always holds for dynamic braking with alternating current of rated frequency. If, however, the frequency is altered so that  $\omega_2 > \omega_1$ , the positive-sequence system gives rise to a braking torque by induction generator action. The use of direct current appears to ensure this condition by making  $\omega_1 = 0$ .

Thus the method of dynamic braking described in the paper may be regarded as a special case of a more general system, and it is suggested that when all factors are taken into account it may occasionally be preferable to use values of  $\omega_1$  other than zero.

I would like to ask the author whether he thinks it is possible that the so-called stray-losses in the induction motor, which contribute to the braking torque, are sufficiently large as to make calculations based on simple equivalent circuits very unsatisfactory? The determination of circuit parameters is a relatively simple matter when compared with the determination of these losses, and the present state of knowledge is such that refinements in calculations from equivalent circuits are hardly justified. It is my experience that the process of obtaining data for accurate torque calculations on induction motors is virtually equivalent to measuring the torques direct. Would the author agree that a considerable amount of research is still required in order to limit the tests which must be performed before an accurate predetermination of the braking performance of a given machine can be made?

**Mr. L. H. A. Carr (communicated):** In Section 4.1 the author points out that it is necessary to correct for the voltage drop in the stator leakage reactance if an accurate value of the transformation ratio is to be obtained from an open-circuited rotor test, and suggests an empirical correction of 5%.

The true correction, which varies largely in individual cases, can easily be obtained from the induction motor circle diagram in the following manner.

If OM represents the magnetizing current, OK the short-circuit or locked-rotor current, and the points M and K be joined, the geometric mean between MK and OK (treated as complex numbers) may be represented by a line BK, the lower termination of which, the point B, lies adjacent to but slightly below the centre point of the line OM.

Using this construction it has been shown\* that for any input current OP the ratio of the equivalent secondary terminal e.m.f. at standstill to the applied primary e.m.f. is given both for magnitude and direction by the ratio PK/BK, a construction that it is believed is well known for the determination of starter resistors, particularly for heavy loads.

Since at standstill with the rotor open-circuited, the point P is coincident with M, it follows that with a one-to-one transformation ratio, the ratio of the open-circuit secondary e.m.f. to the applied primary e.m.f. is MK/BK, which by definition equals  $\sqrt{(MK/OK)}$ .

[The authors' reply to the above discussion will be found on page 133.]

\* CARR, L. H. A.: 'The Circle Diagram of the Induction Motor', *Journal I.E.E.*, 1928, 66, p. 1174.

\* BROWN, J. E., and BUTLER, O. I.: 'A General Method of Analysis of Three-Phase Induction Motors with Asymmetrical Primary Connections', *Proceedings I.E.E.*, Paper No. 1421 U, February, 1953 (100, Part II, p. 25).

† BARTON, T. H.: 'The A.C. Dynamic Braking of Wound Rotor Induction Motors', Ph.D. Thesis, University of Sheffield, 1949, Part II.

## NORTH-WESTERN UTILIZATION GROUP, AT MANCHESTER, 15TH DECEMBER, 1955

**Prof. F. C. Williams:** In a recent paper‡ a new type of variable-speed induction motor was described. One of the features of

this type of machine is that the stator is 'short'; i.e. it is not continuous around the periphery of the machine, but is broken up into blocks each containing a number of poles. Each block can be rotated in such a manner that the direction of motion of the

‡ WILLIAMS, F. C., and LAITHWAITE, E. R.: 'A Brushless Variable-Speed Induction Motor', *Proceedings I.E.E.*, Paper No. 1737 U, November, 1954 (102 A, p. 203).

travelling field produced by it makes an angle  $\theta$  with the direction of motion of the rotor surface.

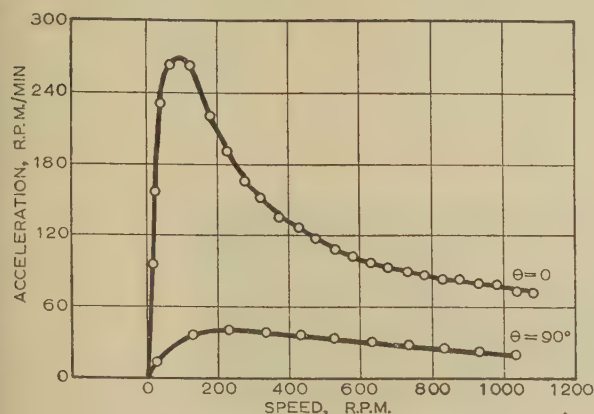


Fig. A.—Dynamic braking of a short-stator induction motor.

Fig. A shows the dynamic braking characteristics for a version of this type of machine having a spherical rotor. The curve for  $\theta = 0$  is very similar to Fig. 11 for a conventional machine as illustrated in the paper. On the other hand, the curve for  $\theta = 90^\circ$  is much flatter and the braking force is much reduced. I should like to know if the author has any comments on these curves which might throw further light on the behaviour of short stator machines.

**Mr. W. Hill:** Unfortunately, the author's approach to the determination of dynamic braking torques is not as intellectually satisfying as a serious student would demand, nor is his graphical construction suited to the modern exigency of streamlined design methods.

In spite of the reference to Kron, the jump from alternator analogy to the induction motor equivalent circuit is a flagrant *non sequitur*, although obviously correct; more correct for dynamic braking actually than for the usual induction-motor operation.

Why not set up the two voltage equations for primary and secondary windings and show that the terminal conditions only determine the operation?

$$\text{If } R_1 i_1 + (L_1 + L_m) \frac{di_1}{dt} + L_m \frac{di_2}{dt} = V_1 e^{j\omega_1 t}$$

$$\text{and } L_m \frac{di_1}{dt} + R_2 i_2 + (L_2 + L_m) \frac{di_2}{dt} = V_2 e^{j(\omega_2 t + \theta)}$$

$$\text{then if } V_1 = V_{\max}; \omega_1 = 314 \text{ rad/sec} \\ V_2 = 0$$

the solution gives ordinary induction-motor operation at 50 c/s.

If  $V_1$  and  $\omega_1$  are as before but  $V_2 = V_{2\max}$  and  $\omega_2 = 0$ , we have the performance of a synchronous induction motor.

$$\text{Finally, if } V_1 = V_{d.c.} \text{ and } \omega_1 = 0 \\ \text{and } V_2 = 0$$

we have the author's dynamic braking conditions and its solution.

Further simplification of the results gives

$$V_g = I_{1d.c.} \frac{1}{Y_m + Y_2}$$

$$\text{and for the torque } T = 3V_g^2 G_2$$

where  $Y_m$  is the magnetizing admittance corresponding to the gap voltage  $V_g$  i.e. saturation is allowed for.

$$Y_2 = G_2 - B_2 j$$

is the rotor admittance corresponding to the rotor impedance

$$\left( \frac{R_2}{\text{fractional speed}} \right) + X_2 j$$

I grieve to see the disappearance of the slip term. The rotor is still slipping from synchronous speed for any speed other than zero.

Under these conditions  $R_2/s$  can be retained,  $s$  being defined as the fractional speed.

Further points of vital importance to the correct design of d.c. braking system have been omitted from the paper, and three of them are as follows.

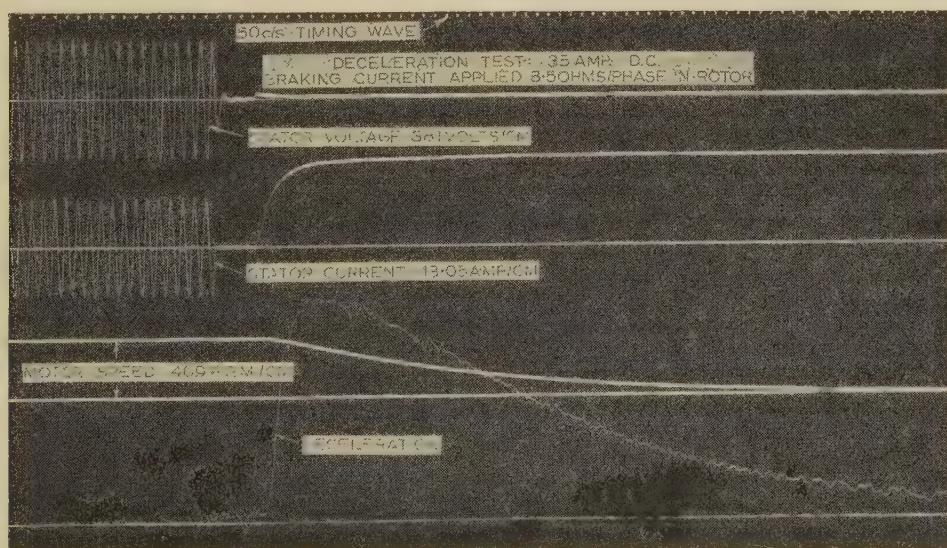


Fig. B.—14 h.p. 3-phase 50 c/s 440-volt 725 r.p.m. induction motor.

(a) Reference to the rate of rise of the direct current at the instant of braking. This is extremely fast because of the coupled secondary circuit (see Fig. B).

(b) When switching over from the a.c. to the d.c. supply rapidly, most of the alternating voltage may still appear at the motor terminals because the original flux has not decayed yet. The insulation of the low-voltage d.c. supply, be it rectifier or motor-generator set, must be suitable for the full 50 c/s alternating voltage (see Fig. C).

obtained was of a much higher order than that to which the braking torques required in most applications can be calculated.

In the Appendix reference is made to connection of stator windings. For star windings (b) is to be preferred to (a) since it is not only a simpler connection but it also results in a lower hot-spot temperature and—important especially on large machines—requires a lower current and correspondingly higher voltage to produce the same effective excitation.

**Mr. E. R. Laithwaite:** In recent experiments on a variable-speed

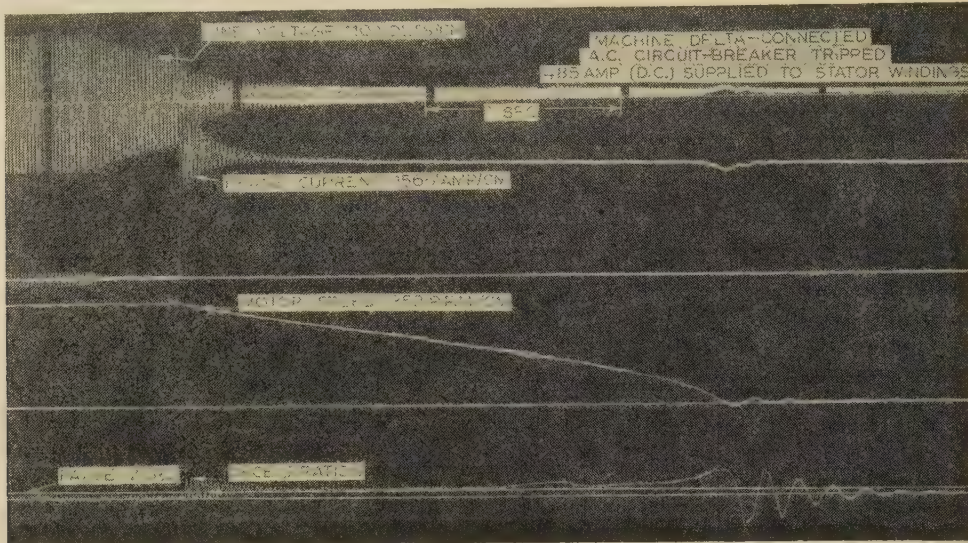


Fig. C.—Speed/torque test on 225 h.p. 3-phase 50 c/s 400/440-volt 975 r.p.m. induction motor.

(c) At and near standstill, locking torques will appear in unskewed slip-ring rotors or in skewed but uninsulated squirrel-cage rotors. This is because the stator and the rotor slots act like small salient poles, attracting each other.

This may cause an oscillatory overshooting of the standstill position (see Fig. C) which may not be tolerated on a precision machine-tool drive.

**Mr. W. H. Laurence:** The author may be interested to know that braking-torque/rotor-resistance curves calculated by means of this method for a 600 h.p. 16-pole motor agreed very closely with results obtained on test some years ago during the development of a system of dynamic braking applied to mine winders.

While his method is theoretically sound and allows for the effect of variable magnetizing impedance on the total impedance of the circuit, it is somewhat more laborious than that described by Mulligan\* which is based on the standard induction-motor circle diagram (a careful study of the induction motor diagram will show that it is the locus of the vector sum of magnetizing and equivalent rotor currents, and if these are based on the corresponding air-gap voltage, the standard diagram is true for the braking condition: the stator reactance will affect only the angle and length of the terminal voltage vector normally taken as reference). Both methods gave values of torque within 2% of test figures over most of the range of excitations and resistances used. The latter, however, requires only one circle diagram to be drawn, whereas the proposed method would require, if confusion is to be avoided, at least as many as the number of excitations to be used. In these calculations the magnetization curve obtained from a no-load run as an induction motor was used. Although no correction to air-gap voltage was made, the accuracy

induction motor, it was necessary to measure the run-down time as a means of estimating the friction and windage loss at different speeds. In the technique developed a circular disc fastened to the motor shaft carries 60 peripheral marks equally spaced. The disc is illuminated by a stroboscopic flash which is synchronized with the supply frequency so as to produce 50 flashes/sec. A stationary pattern is thus obtained every 50 r.p.m. and the stationary patterns may be counted from full speed down to zero.

For run-down of a machine from, say, 1 000 r.p.m. the deceleration between any two stationary patterns may be assumed constant and a decelerating torque/speed curve containing 20 points can be plotted. Other systems of marks may be added to the disc to assist in identifying the particular multiple of 50 r.p.m.

This technique may be applied also to the run-up of a motor in order to plot the speed/torque curves of induction motors, since points can be plotted on the unstable as well as on the stable portions of the curves. The limitation of the system is that the time to accelerate or decelerate between one stationary pattern and the next must be sufficiently long to be measured accurately by the observer and to avoid errors due to transient phenomena. This condition may be achieved either by running at reduced voltage, by attaching a flywheel, or by applying a fixed braking load just less than the machine torque. The dynamic braking curves shown by Prof. Williams were obtained using the run-down technique described.

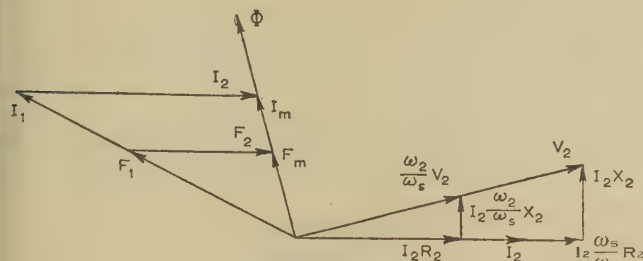
**Mr. N. N. Hancock:** After stating that this is an alternator problem, the author implies that synchronous impedance is the only method of treating dynamic braking by synchronous-machine theory, and proceeds to use the induction-motor equivalent circuit, although this entails some modification which may be disturbing to those not familiar with such techniques. The necessary relations can, however, be derived direct from the usual

\* MULLIGAN, J. W.: 'Dynamic Braking of Slip-ring Induction Motors applied to Mine Winders', *Metropolitan-Vickers Gazette*, June, 1953.

the signs differ from those of the author in the equations derived from it, namely:

$X_m$  is, of course, merely the ratio of the e.m.f. induced in the rotor winding at synchronous speed by the flux produced by a resultant m.m.f. corresponding to a current  $I_m$  to that current.

Whether a graphical method is desirable for actual calculation depends partly upon personal inclination and partly upon the relative magnitudes of the vectors concerned. It is always of great help, however, in understanding the problem, and the author is to be congratulated on producing a new and useful development of his earlier analysis, particularly since the analysis of dynamic braking dates back at least to 1910.



**Fig. D**

## THE AUTHOR'S REPLY TO THE ABOVE DISCUSSIONS

I am sorry that Mr. Carr has misunderstood me. The figure of 5% for the ratio error is not intended as an empirical correction, but merely as an indication of the order for the particular motor used. I am grateful to him for drawing attention to the alternative method of obtaining the true ratio, which I had overlooked.

The curves (Fig. A) referred to by Professor Williams are for an abnormal type of machine, and the unusual shape of the lower curve can be attributed to two factors, in my opinion. The leakage reactance being high, the ratio of peak torque to full-speed torque is low. Again, the presence of circumferential as well as axial rotor circuits gives rise to complex rotor current patterns which result in a variation of effective resistance and reactance with speed, similar to those in a double-squirrel-cage motor.

I cannot see that it is necessary, to obtain steady-state results, to start with differential equations as Mr. Hill suggests.

The use of the term 'slip' was deliberately avoided, as I thought it might lead to confusion. Apparently I was mistaken. I am grateful to Mr. Hill for giving us his experiences in the practical application of d.c. braking, particularly with regard to the slow decay of the alternating voltages.

Mr. Laurence is really suggesting the same approximation as Mr. Krebs. The accuracy of Mr. Mulligan's method depends on the relative magnitude of the stator reactance, but I must emphasize that, if it is used, the primary applied voltage should be used instead of the air-gap voltage.

The apparatus described by Mr. Laithwaite, for measuring run-down times, should be of great value for many applications.

It is quite true, as Mr. Hancock says, that dynamic braking performance can be determined from the alternator vector diagram, but I think there are advantages, for a machine with polyphase windings on stator and rotor, in applying induction-motor techniques.

The mathematical analysis evolved by Mr. Butler is of considerable interest and has advantages over the graphical method for certain purposes. The loss torque due to the induction motor was not measured separately, but the rotor iron-loss torque is quite small, since the air-gap flux is low except at low speeds.

I am glad to learn that Mr. Krebs is in favour of equivalent circuits, but I cannot entirely agree with his suggestion that the stator reactance should be retained. In Section 4.2 it is pointed out that errors in estimating the reactances are compensated to some extent, but not completely. However, the graphical construction (Fig. 5) can be adapted to Mr. Krebs's method,  $V_2$  now being the primary applied voltage, the radius of the circle AB being  $V_2/2(X_1 + X_2)$ . I have calculated by this method the curve corresponding to Fig. 11, and the errors are not inconsiderable, amounting to about 10% for some parts of the curve.

I agree with Dr. Brown that the connections shown in Fig. 13 give equivalent balanced conditions for  $\omega_1 = 0$  only. For other frequencies the equivalent circuits are valid only with balanced stator voltages. The stray losses in the induction motor are unlikely to be appreciable because the flux is low at high speeds.

## FLAT PRESSURE CABLE

By J. S. MØLLERHØJ, M.Sc., Member, A. M. MORGAN, B.Sc.(Eng.), Associate Member,  
and C. T. W. SUTTON, M.Sc.(Eng.), Member.

(The paper was first received 11th October, 1954, and in revised form 28th April, 1955. It was published in July, 1955, and was read before the SUPPLY SECTION 23rd November, 1955, the NORTH-WESTERN SUPPLY GROUP 10th January, and the SOUTH MIDLAND SUPPLY AND UTILIZATION GROUP 13th February, 1956.)

### SUMMARY

The flat pressure cable has a fully-impregnated dielectric which is maintained under a positive pressure by means of a self-contained flexible membrane provided by a specially reinforced lead sheath. A review is given of the history of the development of the cable as well as the construction and the principles on which the cable is designed. The design of the accessories is also considered, and the methods for deriving the current rating of the cable are described. The results of electrical tests show that the cable is suitable for transmission of electrical energy at the highest voltages. Operational experience shows the cable to be completely satisfactory, both electrically and mechanically. Future development of the cable with aluminium sheath and for transmission at direct voltages is discussed. The cable has been used extensively in Europe for both land and submarine use, and to date 109 miles of 3-core cable have been installed.

### LIST OF SYMBOLS

- $d_1$  = Diameter of cable cores, in.
- $f_{max}$  = Limit of proportionality, lb/in<sup>2</sup>.
- $h$  = Maximum height of beam, in.
- $l$  = Depth of laying, in.
- $p$  = Pressure, lb/in<sup>2</sup>.
- $p_{max}$  = Maximum permissible pressure in cable, lb/in<sup>2</sup>.
- $p_{min}$  = Minimum permissible pressure in cable, lb/in<sup>2</sup>.
- $p_1$  = Pressure drop acting across lead sheath, lb/in<sup>2</sup>.
- $p_n$  = Static pressure head, lb/in<sup>2</sup>.
- $r$  = Radius of cable core, in.
- $r_E$  = Equivalent radius of cable, in.
- $t$  = Thickness of screening material, in.
- $t_c$  = Thickness of protective covering, in.
- $E$  = Young's modulus, lb/in<sup>2</sup>.
- $G_E$  = External thermal resistance of cable, °C/watt/cm.
- $G_p$  = Thermal resistance of protective covering, °C/watt/cm.
- $I$  = Second moment of area of corrugated strip, in<sup>4</sup>/in.
- $L$  = Length of major axis of cable, in.
- $P_M$  = Mean periphery of protective covering, in.
- $R_0$  = Resistance of conductor, ohms/cm.
- $R_c$  = Resistance of corrugated strip, ohms/cm.
- $R_s$  = Resistance of sheath, ohms/cm.
- $\gamma$  = Coefficient of expansion of cable, in<sup>3</sup>/in<sup>3</sup>/°C.
- $\Delta v$  = Expansion of cable per unit length, in<sup>3</sup>/in.
- $\Delta v_B$  = Expansion accommodated by beam deflection, in<sup>3</sup>/in.
- $\lambda_c$  = Corrugated strip loss factor.
- $\lambda_s$  = Sheath loss factor.
- $g_c$  = Specific thermal resistivity of insulation, °C/watt/cm<sup>3</sup>.
- $g_e$  = Specific thermal resistivity of soil, °C/watt/cm<sup>3</sup>.
- $g_p$  = Specific thermal resistivity of protective covering, °C/watt/cm<sup>3</sup>.
- $g_s$  = Specific thermal resistivity of screening material, °C/watt/cm<sup>3</sup>.
- $\theta_c$  = Temperature rise of conductor, °C.

### (1) INTRODUCTION

#### (1.1) General

The development of oil-impregnated paper-insulated cables for the transmission of electric power at high voltages has proceeded during the past 30 years along two distinct lines. These developments have been determined by the principle employed for eliminating or reducing any difference in level of electric strength at power-frequency voltages between an oil-impregnated paper dielectric in a virgin condition and one in an aged condition. A large difference occurs with solid-type cables owing to void formation, resulting from cyclic heating of the cable, which consists of an aggregate of materials of varying elastic and thermally expansive properties.

The first principle employed was the prevention of void formation by the application of an hydraulic pressure to the impregnated compound of the dielectric, longitudinally along the cable as in the oil-filled cable, or radially as in the compression cable. Design features used in both of these cable systems were employed in the Oil-o-static cable, which was developed later. All of these cables are classified as using fully-impregnated dielectrics, and have the characteristic of a very high electrical strength at power-frequency voltages.

The second design principle was to allow the formation of voids in the cable, but by introducing gas at high pressure into the dielectric, the electrical strength at power-frequency voltages of these voids can be raised to a sufficiently high level to permit an economical design of the cable at high voltages, but this level may not be as high as with fully impregnated dielectrics. However, special auxiliary equipment is necessary in all these instances to ensure satisfactory operation.

The flat pressure cable eliminates this disadvantage whilst making use of a fully-impregnated dielectric. The flexible impermeable sheath of the cable is reinforced so that the cable is self-compensating to the variations in volume of the dielectric caused by service loading. The design is in accordance with an ideal principle that the cable should be fully self-contained, and it was first produced in Denmark.

In 1950 arrangements were made for a British manufacturer to produce the cable. The stringent test requirements of users in Great Britain and the Commonwealth, particularly the increase of working temperature of the dielectric, led to an intensive development programme by both parties. This necessitated a more detailed study of the working mechanism of the cable, of the method of manufacture and installation, and of the method of rating the cable. The paper sets out the results of this development programme, and gives a brief account of the manufacturing processes together with a description and service experiences of existing installations.

#### (1.2) History of the Development of the Cable

The first flat pressure cable was manufactured in Denmark in 1939, and a commercial installation was completed in 1941 at Aarhus using a 0.15 in<sup>2</sup> 66 kV cable. Further development was retarded during the Second World War. However, in the period

Mr. Møllerhøj is with Nordiske Kabel-og Traadfabriker, Copenhagen, Denmark.  
Mr. Morgan and Mr. Sutton are with Enfield Cables Ltd.

From 1949 onwards a considerable quantity of cable was manufactured. In 1950–51 a 132 kV installation was made comprising in all 5 500 yd of land cable and 6 500 yd of submarine cable, both with 0.3 in<sup>2</sup> conductors, for a connection between Denmark and Sweden at Elsinore–Hälsingborg. This submarine cable was the first 3-core 132 kV submarine cable to be laid in the world. Several installations have also been made for operation at both 33 and 66 kV, and hence the cable design has been used throughout that part of the super h.v. cable range for which the demand is highest. A list of the land cable installations completed to date is given in Table 1. Table 2 is a list of the sub-

marine cable installations. Virtually no design changes were made in adapting the cable for submarine use.

The cable has also undergone an extensive test at the Électricité de France Testing Station at Fontenay. A length of 66 kV cable has been operated, without incidents, since 1952 in their 63 kV network.

## (2) CABLE CONSTRUCTION

### (2.1) General

The construction of the cable has been described previously,<sup>2,3</sup> but it is desirable for completeness to show details of the construction in use at the present. Fig. 1 illustrates a 132 kV

Table 1

#### LAND-CABLE INSTALLATIONS

Date	Location	Voltage	Conductor section	Feeder length
		kV	in <sup>2</sup>	yd
1941	Aarhus .. ..	66	0.15	2 700
1949	Copenhagen .. ..	66	0.15	9 700
	Aarhus .. ..	66	0.15	5 250
	Vejle .. ..	66	0.25	4 700
	Fredericia .. ..	66	0.15	3 000
	Frederiksberg .. ..	66	0.15	4 400
1950	Copenhagen, Elsinore and Hälsingborg ..	132	0.3	5 500
	Copenhagen .. ..	33	0.3	32 000
	Copenhagen .. ..	33	0.1	12 000
	Esbjerg .. ..	66	0.4	3 800
1951	Aalborg-Nørresundby ..	66	0.15	10 000
	Copenhagen .. ..	33	0.1	1 400
	Copenhagen .. ..	33	0.3	8 100
	Odense .. ..	66	0.25	5 600
	Odense .. ..	66	0.075	9 700
	Aarhus .. ..	66	0.2	1 100
1952	Aarhus .. ..	66	0.15	3 300
	Copenhagen .. ..	55	0.4	18 500
	Nyborg .. ..	66	0.15	330
	Aabenraa .. ..	66	0.25	1 850
	Copenhagen .. ..	33	0.1	11 000
1953	Oslo .. ..	66	0.2	2 400
	Copenhagen, Elsinore and Hälsingborg ..	132	0.4	5 000
	Copenhagen .. ..	132	0.4	1 000
	Copenhagen .. ..	132	0.3	3 100
	Copenhagen .. ..	55	0.2	3 100
1954	Thisted .. ..	66	0.15	440
	Aalborg .. ..	66	0.4	220
	Aarhus .. ..	66	0.4	5 500
	Esbjerg .. ..	66	0.15	330
	Aalborg .. ..	66	0.4	3 300
1955	Copenhagen .. ..	132	0.3	12 000
	Copenhagen .. ..	132	0.3	4 500

Table 2

#### SUBMARINE CABLE INSTALLATIONS

Date	Location	Voltage	Conductor section	Feeder length
		kV	in <sup>2</sup>	yd
1949	Svendborgsund ..	66	0.06	1 300
	Guldborgsund ..	66	0.2	770
1950	Copenhagen harbour ..	33	0.3	770
	Copenhagen harbour ..	33	0.1	440
	Øresund .. ..	132	0.3	6 600
1951	Als Fjord .. ..	66	0.25	2 200
	Limfjorden .. ..	66	0.15	1 300
	Copenhagen harbour ..	33	0.1	450
1953	Svendborgsund ..	66	0.05	1 650
	Øresund .. ..	132	0.4	6 200
1954	Sallingsund .. ..	66	0.15	1 750

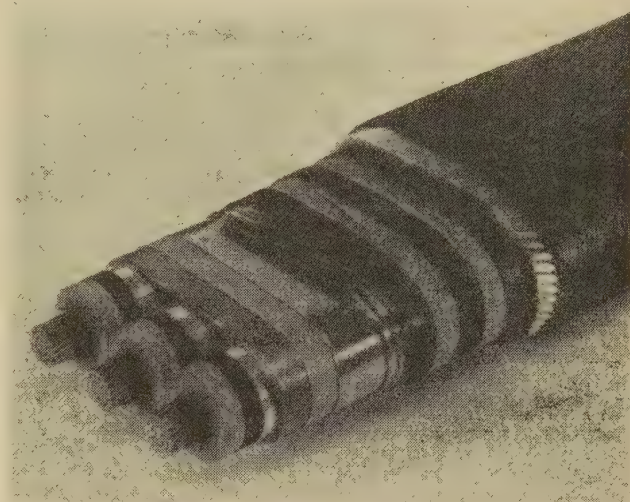


Fig. 1.—Cross-section of 132 kV submarine cable.

submarine cable. Three paper-insulated screened circular conductors laid together side by side in flat formation are enclosed in a lead sheath, reinforced with metal tapes applied circumferentially together with two corrugated metal strips which are bound on to the flat faces by means of one or more wires. The insulation is impregnated with a low-viscosity oil after sheathing. Anti-corrosion protection is applied overall in the normal manner, together with jute serving and aluminium-alloy armouring.

### (2.2) Details of Constructional Design

#### (2.2.1) Conductor.

The circular-cross-section conductor consists of copper wires stranded to standard specification. The individual wires were tinned in the original designs to minimize any difficulties which might have arisen in soldering cable ferrules at joints, etc. Subsequent experience has proved this to be unnecessary, and plain copper wires are now used. Cables have been installed with both screened and unscreened conductors. Experience with other cable designs has shown that the impulse and a.c. strength of the cable could be improved by 20% by the application of paper tapes containing colloidal carbon, known as CB screening, over the plain copper conductor. The CB screening has been used since the end of 1952, and in all, 18 miles of 33, 66 and 132 kV flat cable with this screening are in operation.

Manufacturers in Great Britain have been reluctant to use conductor screening, owing to a fear on the part of the users that manufacturing defects in the screen might lead to the production of "stress raisers" on the surface of the conductor. This view might be due to the association of metallized paper

tapes with conductor screening, where the conducting screen is in the form of a thin film on the surface of normal insulating paper. With CB screening the semi-conducting property is not localized but exists throughout the whole depth of the tape. The production of a "stress raiser" is then much less likely with this type of screening, even if the argument against screening with metallized paper tapes were accepted.

#### (2.2.2) Dielectric.

The dielectric is composed of paper tapes lapped onto the conductor in the usual manner. High-grade wood-pulp paper has generally been used in tapes of normal thickness. There has been a tendency on the part of some Continental manufacturers to use paper having a thickness of less than 1 mil, in order to enable higher design stress to be used. Whilst this might well be a practical solution for cables operating at 220 or 380 kV line voltage, the adoption of this practice brings attendant problems in the more precise design and the increased size of the paper lapping machinery required. Furthermore, it has been found necessary to use paper of a composition different from that used in the remainder of the dielectric. The application of this technique has not been necessary for cables in the range 33–132 kV working voltage.

The 132 kV flat cables have been insulated with papers of varying thicknesses, ranging from 2.5 mil at the conductor to 5 mil at the outside of the core. An experimental design of 220 kV cable has been based on similar insulating practice.

The impregnating compound is a refined naphthene-base oil with an additive. The viscosity characteristic is shown in Table 3. The physical characteristics are such that the compound has sufficiently low viscosity at the temperature of

Table 3  
VISCOSITY OF IMPREGNANT

Temperature	Viscosity
°C	centistokes
20	170
50	33
100	3.9

impregnation to allow the impregnating process to be carried out after application of the cable sheath, and yet at the same time the viscosity is sufficiently high at ambient temperature to permit the jointing of the cable to be carried out with little modification to the normal practice for solid cables.

#### (2.2.3) Lead Sheath.

The lead sheath is applied by means of a rectangular point and die. The sheathing material is extruded either on a screw press or on a ram-type press, both of which produce a uniformly thick oxide-free sheath.

The lead sheath acts as a membrane and undergoes cyclic straining corresponding to the variations of the daily load cycle. Particular attention must be paid to the possibility of failure of the lead sheath owing to fatigue, especially with regard to the method of extrusion and the material used. Some cables were sheathed with a copper-lead alloy containing 0.06% copper, which gave a small crystal structure. The results of further experience and a prolonged series of life tests conducted in the laboratory showed that commercially pure lead, which contains 0.03–0.05% of copper, or alternatively an alloy containing 0.2% tin and 0.075% cadmium, which gives good results in conditions of installation where the cable undergoes prolonged vibration in addition to the cyclic straining, could also be used.

The high standard of performance called for by the Cable Makers Association and the Central Electricity Authority and Area Boards necessitated a careful analysis of the strains occurring throughout the lead sheath. The results of these investigations showed that the maximum strain, resulting from daily and seasonal load cycles together with a direct tensile strain produced by the pressure in the cable, was not sufficient to cause a failure of the lead sheath during the life of the cable.

The method of testing the lead sheaths was to pump oil into cable samples to produce a maximum expansion of the cable corresponding to a 120°C conductor temperature rise. An arrangement of cams permitted the rate of expansion to be controlled to simulate the load cycle on the cable. These tests produced a maximum strain of 0.3% on the lead sheath, which is 70% higher than the maximum figure for a cable in service, assuming a maximum temperature of 85°C. The frequency of test cycles for the first series of tests was 1 cycle/min, and the minimum sheath life obtained before failure occurred was 18 000 cycles. Further tests at a frequency of 1 cycle every 10 min are in progress.

An obvious difficulty in designing a fatigue test for cable sheaths is that the frequency of the test cycles must of necessity be much higher than the frequency of loading cycles in service, so that test results can be obtained in a reasonably short time. Making allowances for this increase of test-cycle frequency and also the additional strain imposed, a figure is obtained of 13 000 cycles minimum sheath life in service. With 300 cycles per year this means that the sheath life in service will be at least 40 years.

The cable was originally manufactured so that during the extrusion of the lead sheath the axis of the ram of the lead press was in line with the major axis of the cable. This practice was guided by the belief that the central portion of the membrane was subjected to the maximum strain and was the weakest part of the sheath. In modern practice the sheath is extruded with the cable aligned in the normal manner. This change was brought about by a number of considerations. The points at the ends of the major axis of the cable sheath, which coincide with the position of the weld in the sheath, are subjected to maximum shearing strain. Also the analysis and experiments referred to previously in this Section of the paper show that the weakest point of the sheath was not at the centre of the membrane section. Improvement in efficiency of manufacture has produced a sheath, in which the welds have mechanical properties equivalent to the rest of the sheath, as proved by numerous bending and pulling tests on sheath samples. This change has resulted in a saving of manufacturing-shop floor space and also in the alignment of manufacturing procedure with that in normal use for other types of cable.

#### (2.2.4) Reinforcement.

Two non-ferrous metallic strips are applied circumferentially edge-to-edge and breaking joint on a bedding of impregnated paper tapes. These are followed by the corrugated strips which are bound down with two non-ferrous wires; these wires run into the corrugations and form a double screw line.

Bronze was originally used throughout the whole of the reinforcement, but recent investigations both in Denmark and Great Britain have shown that half-hard copper can be used for the circumferential reinforcement and the binding wires. The corrugated strip is made with bronze containing 5% tin, and in some cases 6% tin-bronze has been used. The absence of any noticeable creep, the good elastic behaviour and the high limit of proportionality of these materials make them highly suitable for constructing a pressure-controlling membrane.

The double-screw line of binding wires gives a degree of

protection to the cable. Should one wire fail, the second wire will retain the corrugated strip in the correct position on the sheath. The pitch of each corrugation for larger cables is  $\frac{1}{2}$  in. For smaller cables requiring thin corrugated strips it has been possible to use a  $\frac{5}{32}$  in pitch and to lay the binding wires in alternate grooves.

The corrugation of the strip ensures good flexibility with bending of the cable about the major axis and at the same time ensures that a maximum stiffness of the strip for bending about the longitudinal axis of the cable is achieved with a minimum of material. Improvements in the design of the forming wheels of the machines used for manufacturing the strip have resulted in further increases in the stiffness obtained with a strip of given thickness and height of corrugation.

### 2.2.5) Submarine Armouring.

The first flat-type submarine cables were armoured with copper wires and no external jute serving was used. The high price of copper made it necessary to use an alternative material. Present practice is to use galvanized steel wires for small cables, where the added reactance is comparatively small.

For larger cables, or when a reduction in weight of the cable is of particular importance, aluminium-alloy wires containing 2% magnesium and 0.25% manganese are used. In both cases double jute serving is applied over the armouring.

### 2.2.6) Anti-Corrosion Protection.

The essential feature of the cable design is the corrugated strip, and this strip must be protected against any possible corrosion. The corrosion protection used both in Great Britain and Denmark has been the so-called "rubber sandwich" protection. This protection generally consists of two separate impermeable layers each built up with rubber tape, bitumen-impregnated cotton tape and bitumen. Up to the present, no failures have occurred owing to corrosion.

## (3) CABLE DESIGN

### (3.1) General

The design of the insulation of a high-voltage cable is usually based on the maximum electric stress at the conductor. For the higher-voltage ranges it is the electric strength to power frequency or impulse voltages which is the governing factor, depending upon the test requirements for the cable. An exception occurs with the lower-voltage cables, where the mechanical characteristics of the thin dielectric determine the design stress. The Central Electricity Authority employs an empirical formula<sup>4</sup>  $4.5(V + 10)$  kV for the impulse test requirement, where  $V$  is the nominal system voltage between phases in kilovolts, which corresponds to factors of 10, 9, and 8.4 times the working voltage at 33, 66 and 132 kV respectively. Generally it can be stated that the variation in impulse strength of all the cables mentioned in Section 1.1 of the paper is not more than 10–20%. The ratio of impulse to power-frequency electric strength for a fully-impregnated dielectric lies within the range 2.5 : 1 to 3.5 : 1, and hence with cables of this type the impulse strength will determine the design stress for the cable. Any advances made in the improvement of impulse strength of cable dielectrics will result in more economical design for these types of cables, since there is a large margin between the ultimate long-time breakdown stress and the working stress at power-frequency voltages.

### (3.2) Basis of Electrical Design of Cables for Various Voltages

#### (3.2.1) Cables for 33–132 kV.

Special cable designs can be justified at voltages as low as 33 kV. A considerable quantity of this cable was installed in Copenhagen in 1950–51.

The dielectric design was based on a nominal maximum stress at the conductor of 75 kV/cm or 0.13 in minimum radial thickness, whichever gives the greater insulation thickness.

A maximum stress of 750 kV(peak)/cm is produced at the conductor with the impulse-withstand test-voltage requirement of 194 kV(peak).

At 66 kV the design stress can be increased to 80 kV/cm. The maximum stress at the conductor surface for an impulse test voltage of 342 kV(peak) is 720 kV(peak)/cm.

For 132 kV the design stress is increased to 85 kV/cm, giving a maximum stress at the conductor surface of 720 kV(peak)/cm at the test voltage of 642 kV(peak). By using CB screening over the conductor and thin papers, as already mentioned in Section 2.2.2, the design stress can be raised to 100 kV/cm, and there is at present cable in service with unscreened conductor designed on this maximum stress.

#### (3.2.2) 220 kV Cable.

An experimental design of cable for 220 kV has been developed where the conductor has been screened with CB paper tapes, and thin papers are employed so that a design stress of 125 kV/cm can be adopted to meet the impulse test level of 1050 kV. The optimum conductor size for a cable at this working voltage is 0.4 in<sup>2</sup>.

### (3.3) Basis of Mechanical Design of Cables

The original design methods for the cable introduced certain approximations. More widespread application of the cable necessitated a more careful study of the mechanism by which the variations of volume of the cable contents with variations of dielectric temperature was accommodated. The present design practice produces a more economic construction.

#### (3.3.1) Accommodation of the Increase in Volume of the Cable Contents

The mechanical deformation of the cable was measured in a special gauge for variations of internal pressure, produced hydraulically and also thermally. Many designs of cable for various working voltages were studied, and the results were found to be in close agreement with an analysis of the mechanical deformation of a flat-sided cylinder with semi-circular ends composed of elastic material, subjected to an internal pressure. The semi-circular ends were considered to be restrained against inward movement by a force which was a function of the magnitude of that movement. The latter consideration represents the action of the insulation of the two outer cores against movement of the cable sheath. The flat sides were considered to possess a stiffness which was great compared with that of the semi-circular ends.

The results of this investigation revealed that when the cable expands owing to the rise of temperature produced by the application of loading current through the conductors, the increase of volume of the cable contents is accommodated by a number of different mechanisms.

(a) The dielectric is compressed owing to an increase of pressure within the cable.

(b) The lead sheath and reinforcement are extended by thermal expansion.

(c) The lead sheath and reinforcement are extended by an increase of internal pressure producing tensile forces in these members.

(d) The flat sides are bent in a manner similar to that of a uniformly loaded beam.

(e) The semi-circular ends are bent to a greater radius of curvature, producing an outward displacement of the flat sides.

Typical results of measurements are shown in Fig. 2, from which it is seen that half of the total volume increase due to expansion of the cable contents is accommodated by deflection of the flat side as a uniformly loaded beam; this is in agreement with the results of analysis.

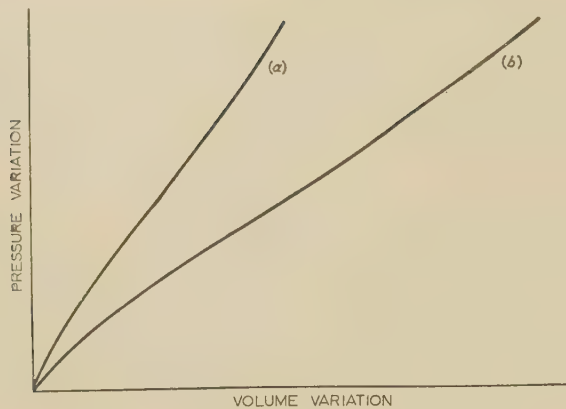


Fig. 2.—Volume-increase/pressure relationship.

(a) Uniformly loaded beam.  
(b) Total.

### (3.3.2) The Coefficient of Expansion of the Cable Components.

In order to calculate the expansion of the cable contents produced by a rise of conductor temperature, use is made of an equivalent coefficient of expansion for cable which is referred to the temperature rise at the conductor. The coefficient is defined as the increase in volume of cable contents per unit length per unit temperature rise at the conductor divided by the volume of the cable contents per unit length. In deriving this coefficient the following assumptions were made:

- The oil channels between the core and the sheath were assumed to be at the same temperature as the lead sheath. Experiments described in Section 6.1.1 to establish data for current rating of the cable showed this assumption to be reasonable.
- The temperature drop across the dielectric between conductor and metallized screen followed a hyperbolic distribution.
- The oil in the strand interstices was at the same temperature as the conductor.

After investigation of various cable sections it was found that, when the cable was laid directly in the ground, the coefficient of expansion for the cable,  $\gamma$ , was half the coefficient of expansion of the cable oil, i.e.  $\gamma = 3.5 \times 10^{-4} \text{ in}^3/\text{in}^3/^\circ\text{C}$  at  $g_E = 120^\circ\text{C}/\text{watt}/\text{cm}^3$ . Submarine cables have a lower value of  $\gamma$ , i.e.  $3.0 \times 10^{-4} \text{ in}^3/\text{in}^3/^\circ\text{C}$  at  $g_E = 40^\circ\text{C}/\text{watt}/\text{cm}^3$ , owing to an increased dielectric temperature gradient.

### (3.3.3) Corrugated Strip.

The satisfactory operation of the cable depends upon the design of the corrugated strip. The maximum temperature rise of the conductor,  $\theta_c$ , is determined from the difference between the maximum operating temperature,  $85^\circ\text{C}$ , and the minimum temperature, allowance being made for the seasonal variations of ambient temperature. The expansion to be accommodated,  $\Delta v$ , will be given for land cables by

$$\Delta v = 9.75 \times 10^{-4} \theta_c d_1^2 \quad (1)$$

where  $d_1$  = diameter of cable cores.

For submarine cables the coefficient in eqn. (1) would be 8.35 instead of 9.75.

Section 3.3.1 shows that half the expansion was accommodated by the corrugated strip acting as a uniformly loaded beam deflected by a pressure  $p$ . This pressure is given by

$$p = p_{\max} - (p_{\min} + p_1 + p_n) \quad (2)$$

where  $p_{\max}$  = Maximum permissible pressure in the cable.

$p_{\min}$  = Minimum permissible pressure in the cable.

$p_1$  = Pressure drop acting across the lead sheath during the condition of sudden removal of load.

$p_n$  = Static head of pressure when the cable is installed on a slope.

The normal working values are as follows:

$$p_{\max} = 90 \text{ lb/in}^2$$

$$p_{\min} = 7 \text{ lb/in}^2$$

$$p_1 = 15 \text{ lb/in}^2$$

The value of  $p_{\max}$  is governed by the permissible permanent deformation of the cable dielectric at the ends of the major axis. Excessive deformation could arise if the corrugated strip were designed with too great a stiffness. The value of  $p_{\min}$  is chosen so that the dielectric will always remain fully impregnated. The choice of  $p_1$  allows for all grades of sheathing material, and would be smaller in the case of commercially pure lead than for the various alloys in use.

The length of the corrugated strip when considered as a beam of unit width will be  $2d_1$ , and hence the volume accommodated,  $\Delta v_B$ , by deflection of the beam can be shown to be

$$\Delta v_B = \frac{0.535 p d_1^5}{EI} \quad (3)$$

where  $E$  = Young's modulus for the corrugated strip material.

$I$  = Second moment of area of the beam section about the axis of bending.

Since

$$\Delta v = 2\Delta v_B$$

$$I = \frac{1.1(68 - p_n)d_1^3 10^3}{\theta_c E} \quad (4)$$

The actual design of the strip can now be decided upon by reference to Fig. 3, which shows the variation of  $I$  with depth of

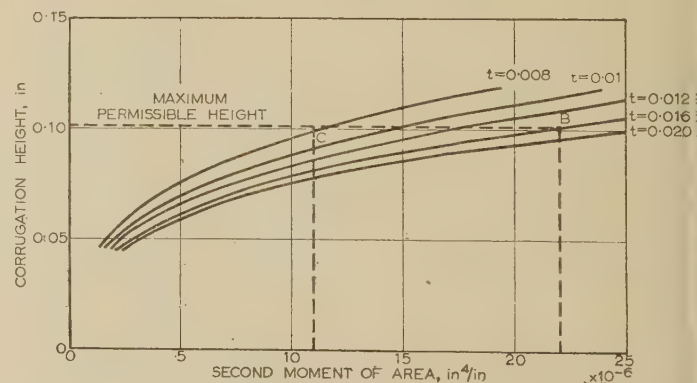


Fig. 3.—Second moment of area of corrugated strip sections.

Pitch of corrugations:  $\frac{5}{8}$  in.

corrugation, and thickness of strip. A large number of designs could be selected from Fig. 3 which would satisfy eqn. (4), but further considerations lead to a definite choice.

The strip must be designed so that the material is not strained beyond the limit of proportionality at any point when the strip is subjected to the maximum pressure. The points which undergo the maximum strain are the outermost fibres. The maximum pressure acting across the strip is taken as  $80 \text{ lb/in}^2$ , i.e. the maximum pressure within the cable less  $10 \text{ lb/in}^2$ , in order to

allow for a certain amount of the pressure being restrained by the lead sheath and other reinforcement.

The maximum permissible height of the corrugated section will be given by

$$h = \frac{f_{max} I}{20d_1^2} \quad (5)$$

The value for  $f_{max}$ , the limit of proportionality, varies with the type of material used. It is 57 000 lb/in<sup>2</sup> for half-hard 5% tin-bronze, and 74 000 lb/in<sup>2</sup> for fully-hard 6% tin-bronze.

In general, as deep a section as possible should be used, since this will mean that the thickness of the strip will be a minimum, and it will lead to an economy in material and reduction of electrical losses in the reinforcement. A reference to Fig. 3 illustrates the consideration which determines the final selection of strip size. If the strip size selected is represented on the curves by the point B, then at this point there is a large variation of  $I$  with a small variation of depth, and the precision of manufacture of the corrugated strip must be accordingly high. Consequently, it might be preferable to select a strip section as chosen at C, where  $I$  does not vary greatly with depth of corrugation.

#### (3.3.4) Lead-Sheath Thickness.

The lead-sheath thickness is the same as for other designs of cable, i.e. 0.08–0.12 in according to the size of the cable.

#### (3.3.5) Reinforcing Tapes.

The thickness of the circumferential reinforcing tapes is chosen so that the maximum static pressure is adequately supported in accordance with design principles in practice for cables with internal gas or oil pressure. It should be noted that there is no need to design these tapes to support transient pressures in excess of the maximum static pressure, as will be evident from Section 5.2.4.

#### (3.3.6) Corrugated-Strip Retaining Wire.

The size of binding wire chosen is adequate to support the maximum pressure acting on the corrugated strip.

### (3.4) Typical Designs

Table 4 shows dimensions of the 0.4 in<sup>2</sup> 132 kV submarine cable laid between Denmark and Sweden in 1954, as shown in Fig. 1.

Table 4

DESIGN OF 0.4 IN<sup>2</sup> 132 kV ØRESUND CABLE

Conductor cross-section .. .. .	240 mm <sup>2</sup>
Diameter over conductor .. .. .	20.2 mm
CB Screening—Number of layers at conductor ..	3
Thickness of layers .. .. .	0.125 mm
Number of layers over core .. .. .	2
Dielectric thickness .. .. .	11.4 mm
Diameter of insulated core .. .. .	43 mm
Lead sheath thickness .. .. .	3.0 mm
Bedding of two oiled papers	

#### Reinforcement

Thickness of non-ferrous tape .. .. .	0.15 mm
Number of tapes .. .. .	2
Number of corrugated tapes per side .. .. .	2
Height of corrugation .. .. .	3.5 mm
Pitch of corrugation .. .. .	6.6 mm
Number of binding wires .. .. .	2
Diameter of wires .. .. .	1.8 mm
Jute serving and rubber sandwich protection	
Aluminium alloy armouring, wire diameter ..	5 mm
Number of wires .. .. .	54
Jute serving	

### (4) CABLE MANUFACTURE

The manufacturing processes differ little from those used for paper-insulated cables. Each cable core is dried with the application of vacuum in three drying tanks.

The three cores are fed side by side into the lead press, and operations are arranged so that there is a minimum exposure of the dried cores before lead sheathing. The sheathed cable is taken up on to a drum in the normal manner, except that the drum is moved backwards and forwards along its axis so that the position of the cable relative to the die box remains constant. Mention should be made of the absence of a laying-up operation as is normal with other types of 3-core cables, and which very often limits the maximum length of cable which can be manufactured. Considerably longer lengths of 3-core flat pressure cable can be manufactured than for most other types of 3-core cable.

The dielectric is now subjected to further drying and then impregnated with oil to a pressure of 5 lb/in<sup>2</sup>. This oil pressure is retained during the subsequent reinforcing, armouring and serving operations. The corrugated strips are produced as required by the operatives on a small machine located adjacent to the reinforcing machines.

### (5) ACCESSORIES

The cable accessories follow the orthodox designs for paper-insulated cable and present no special difficulties in design.

#### (5.1) Electrical Design

The joints and sealing ends are designed electrically so that the electric stresses are controlled at the weakest points in the design.

In joints the cable dielectric on either side of the ferrule is tapered smoothly so that the average longitudinal stress is controlled to 2.5 kV/cm for voltages between 33 and 132 kV. The stepping of the chamfer used in some other designs of high-voltage cable has not been considered necessary. The dielectric of the joint is then built up with hand-applied paper tapes to an insulation thickness such that the maximum electrical stress in the joint is not greater than 75% of the design stress of the cable. The transition between the diameter of the cable dielectric and the diameter of the joint dielectric is smoothly tapered to give an average longitudinal stress of 2.5 kV/cm. Again no specially-shaped profile is used.

The additional insulation for the sealing end at the termination of the metallized paper screen is built up in the normal manner with pre-impregnated paper rolls. The rolls are preshaped to give a profile along which the nominal longitudinal stress is 2.5 kV/cm. The diameter of the preshaped cone is terminated when the nominal radial stress is 9 kV/cm. The choice of this figure is purely empirical. The location of the stress cone within the sealing end is such that as high a flashover voltage as possible is obtained when the termination is subjected to impulse tests, consistent with a sufficiently high internal creepage distance between cable guide and stress cone to prevent internal breakdown.

The 220 kV cable uses a condenser cone of established design,<sup>5</sup> which is manufactured in the factory and slipped over the end of the cable on to a hand-applied paper buffer. Two cones in series using an effective length of grading of 71 in have been used.

The sealing ends for all voltages are filled with the same compound as used in the cable dielectric, after evacuation to a good degree of vacuum.

#### (5.2) Mechanical Design

##### (5.2.1) Joint Design.

The application of the cable for both land and submarine use has necessitated the design of three different types of joints.

The joints are all designed electrically as described in Section 5.1, but there are certain differences in mechanical design.

A straight joint for land use is shown in Fig. 4. Two glands are plumbed onto the flat lead sheath. These glands are of

splice practice," which is well known from telegraph-cable repair work. Finally, the whole armoured joint was covered with a tight binding of bituminized seaman's yarn.

The rigid joint is of a conventional design. A copper joint

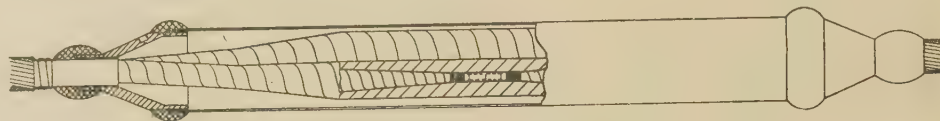


Fig. 4.—Straight joint for land cable.

such a shape as to permit the use of a circular jointing sleeve under which the cores can be transposed, if required. The tube is rigid and does not contribute to the membranous compensating action. The ends of the cable adjacent to the joint are reinforced with additional corrugated strip to ensure that the cable sheath next to the wipe and the wipe itself is not subjected to any fatigue. The conductors are jointed by a flush-fitting sweated ferrule. The joint is filled with oil, as used to impregnate the cable, by the normal vacuum filling process.

sleeve is used, the internal construction being similar to that used for land cables. The armour wires are anchored in conical span rings, the joint end-plates are staggered with four steel bolts, the whole joint is enclosed in a bipartite steel sleeve, and the sleeve is filled with bituminous compound.

Installation of the cable on inclines with large level differences might introduce the necessity of using stop-joints. These are of a straightforward design, and are similar to designs in use for many years with oil-filled cables.

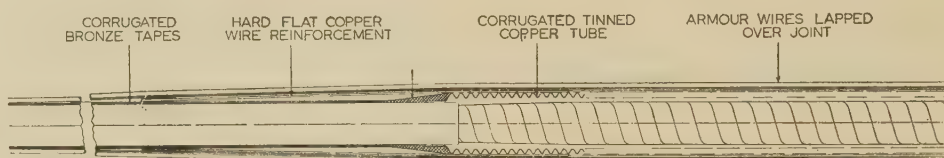


Fig. 5.—Flexible joint for submarine cable.

When the cable is used for submarine purposes two additional joint designs must be provided.<sup>6</sup> A flexible joint, as shown in Fig. 5, which is used when the cable is laid by the Pluto technique. A rigid joint, as shown in Fig. 6, is provided for making repairs to the cable in the event of a failure, as for jointing the cable at sea.

The flexible-joint insulation is completed in the normal manner, but the cores are retained in flat formation. A flat-section corrugated tinned copper tube is applied over the cores and soldered to the cable sheath. The same graduated reinforcement of the membrane armouring by means of stepped additional corrugated strips, as used for land cables, is applied adjacent to the tube. The wipes between the tube and the cable, and the adjacent section of cable and tube are stiffened by a layer of short flat copper strips or wires, arranged lengthwise, and bound by bronze or copper wire bindings. To prevent any membrane movement of the flat copper tube, without impairing the bending flexibility, the flat sides of the tube are reinforced by heavy corrugated bronze tapes. The re-establishment of the cable armouring and the armouring of the joint is made by means of the superfluous lengths of armouring wires from the two cable ends provided by suitable overlapping and resulting from the cutting away of a predetermined length of the unarmoured cable after bending back the armouring wires.

Upon completion of the internal copper-tube joint and the various bituminizing and lapping with textiles, insulating tapes and corrosion protection, the armouring wires are laid back around the cable and the joint from each side in turn to a certain distance beyond the joint and secured with 4mm aluminium bindings at approximately 2ft intervals. Before application of the second layer of armouring wires the first layer is covered with bituminized tape. The second layer of armouring wires is also bound with 4mm bindings at approximately 2ft intervals, the principle being more or less in accordance with the "overhaul

#### (5.2.2) Sealing Ends.

A typical arrangement of the sealing ends of a 3-core termination is to take the cable into the trifurcating box, after stripping the lead sheath and reinforcement. The cable is reinforced by additional corrugated strip adjacent to the trifurcating box, and the lead sheath and reinforcement are plumbed to the trifurcating box, in a similar manner to that used in the construction of a straight joint. The three cores of the cable are covered with copper-woven linen tapes and then threaded into copper tubes. The tubes are plumbed to the trifurcating box by means of hand-wiped joints at one end, and plumbed to the base casting of the sealing ends.

Sealing ends of single porcelain construction are used throughout. The seal between the lower metal casting and the porcelain is made by either "O" rings or by controlled compression gaskets of conventional design.

The assembly, insulation and filling of the sealing end is made at ground level with the structure in a horizontal position. The structure is hoisted into the vertical position after completion.

#### (5.2.3) Oil-Pressure Control Equipment.

The ancillary equipment consists solely of a pressure gauge with electrical contacts to give maximum and minimum pressure alarms, and a valve to allow introduction of oil during the filling of the sealing ends. All of this equipment is fitted directly on the trifurcating box.

#### (5.2.4) Compensation of Expansion of Oil in Sealing Ends and Joints.

No special equipment is required to allow for compensation of the expansion of the oil in rigid sealing ends and joints, as is the case in other cable designs. The cable section close to the joint and sealing ends caters for this by a very small increase of deflection of the membranous flat side of the cable sheath. On cooling of the cable the oil passes back into the joint sleeves and sealing ends.

Calculations and experiments showed that the transient pressure rise in the sealing ends or joints under conditions of sudden application or removal of load was negligible.

#### (6) CURRENT RATING

Very little information has been published on the subject of current rating of the cable. Analysis and experiment has now placed the calculation of the current rating of the cable on a logical basis, as is the case for other types of cable with circular sheaths.

The procedure adopted is in line with current practice, each element of the regions through which heat flows from the source at the conductor to the sink at ground level, etc., has been treated as having a lumped characteristic. The current ratings obtained are similar to other designs of 3-core super-h.v. cable.

#### (6.1) Thermal Resistance of the Various Elements

##### (6.1.1) Thermal Resistance of Dielectric.

The geometrical arrangement of the cables indicated that the central core would have a higher maximum operating temperature than the outer two cores, and therefore the rating was based on the thermal resistance of the central core. When precise mechanical design is necessary the temperature rise of the outer cores must be known, so that data are required for both the outer and the inner cores.

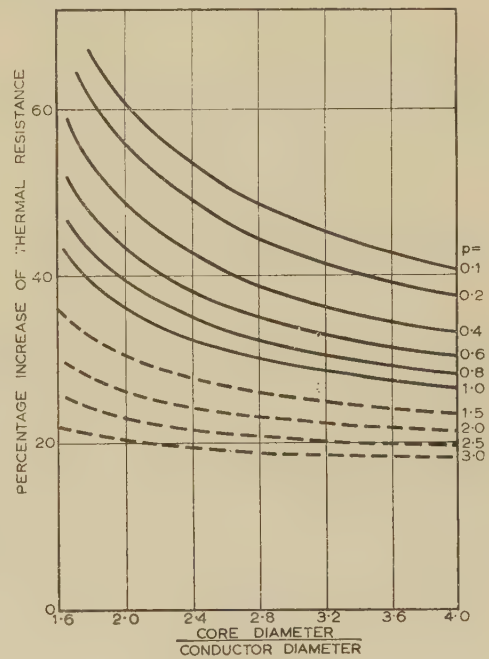


Fig. 7.—Geometry factor for thermal resistance of dielectric.

Fig. 7 shows curves for the central core relating a geometry factor for various values of the screen conductivity factor  $p$ . The factor is defined as the ratio of the actual thermal resistance to the thermal resistance obtained by considering a uniformly radial heat flow within the core. It is defined by

$$p = \frac{g_c t}{2g_s r} \quad (6)$$

where  $g_c$  = Specific thermal resistivity of insulation.  
 $g_s$  = Specific thermal resistivity of screening material.  
 $t$  = Thickness of screening material.  
 $r$  = Radius of core.

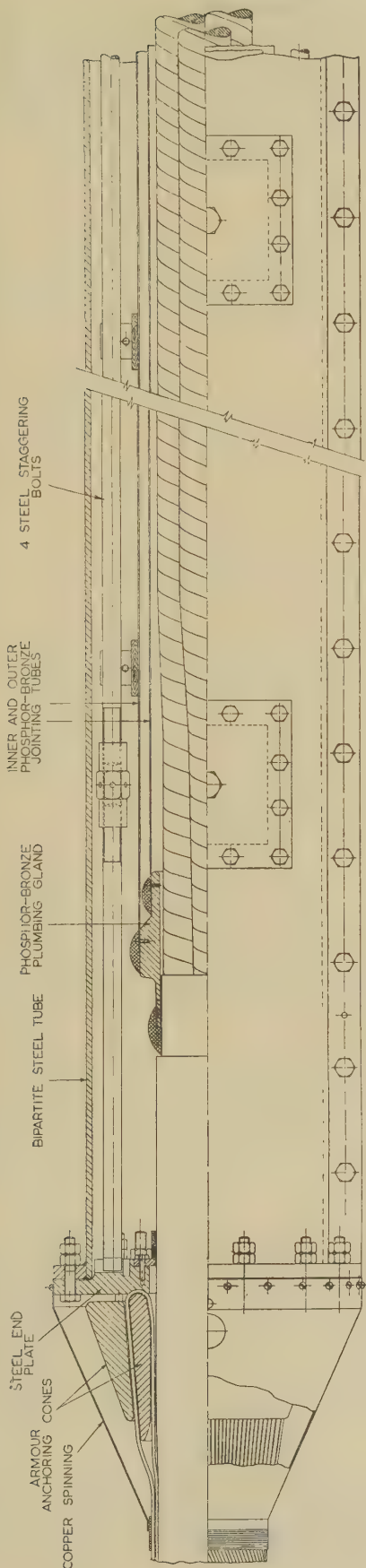


Fig. 6.—Rigid joint for submarine cable.

The curves were verified by three independent methods. The first set of measurements were made in Denmark with an electrolytic model, as had been used for measurements on 3-core H and HSO cables,<sup>7</sup> but using an a.f. voltage source instead of a d.c. source in order to eliminate errors due to polarization effects. A second set of measurements made in Great Britain used a Eureka-wire model,<sup>8</sup> and the third set, also made in Great Britain, employed a sheet-Eureka model.<sup>9</sup> All measurements were in substantial agreement.

Results of a test during which a 66 kV cable was loaded, and the temperature of the outer and central cores were measured, are shown in Fig. 8. This shows that, practically, there is only a small difference of temperature between the outer and central cores.

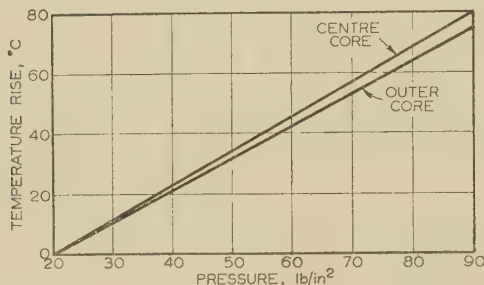


Fig. 8.—Temperature of outer and central cores.

#### (6.1.2) Bedding and Protective Coverings.

The thermal resistance of the protective covering,  $G_P\gamma$ , is given by

$$G_P = \frac{g_P t_c}{P_m} \quad (7)$$

$g_P$  = Specific thermal resistivity of the covering.

$t_c$  = Thickness of the covering.

$P_m$  = Mean periphery of the covering.

Calculation of the heat flow through relatively thick coverings using relaxation methods and an electrolytic model showed that this formula was correct to within a few per cent.

#### (6.1.3) External Thermal Resistance.

The external thermal resistance,  $G_e$ , between cable and ground has been calculated on the basis of heat flow between an elliptically-shaped cylinder located a discrete distance from a plane surface in a semi-infinite medium. The result can be expressed in terms of the normal formula used for a circular-sheathed cable

$$G_e = \frac{g_e}{2\pi} \log_e \left( \frac{2l}{r_e} \right) \quad (8)$$

$g_e$  = Specific thermal resistivity of the soil.

$l$  = Depth of laying.

$r_e$  = Equivalent radius of cable.

For normal depths of laying  $r_e$  is related to the length of the major axis of the cable  $L$  by

$$r_e = 0.362L \quad (9)$$

When the cable is laid close to the surface  $r_e$  is dependent upon the depth of laying and

$$r_e = \frac{0.723L}{1 + \left(1 + \frac{L^2}{3.82l^2}\right)^{1/2}} \quad (10)$$

#### (6.2) Conductor, Sheath and Armour Losses

The conductor, sheath, reinforcement and armour losses were determined by measurements with a modified Thomson bridge. The armour and reinforcement was stripped down, and by the difference between the apparent a.c. resistance of the conductor at each stage, an experimental check was obtained of the loss factors shown in the following Sections.

##### (6.2.1) Conductor Losses.

The losses occurring in the conductors owing to skin and proximity effects for a 3-core arrangement in flat formation have already been calculated elsewhere. When the conductors are transposed at joints the difference between the average loss and the losses with three cores in triangular formation is not greater than a fraction of one per cent.

##### (6.2.2) Sheath and Reinforcement of Non-Ferrous Armour Losses.

The sheath losses can be expressed as a fractional increase of conductor resistance by a factor

$$\lambda_s = \frac{0.093\omega^2 \times 10^{-17}}{R_0 R_s} \quad (11)$$

where  $\omega = 2\pi$  times the frequency of supply, c/s.

$R_0$  = Resistance of conductor, ohms/cm.

$R_s$  = Resistance of sheath, ohms/cm.

This formula was obtained by a semi-graphical analysis, and it is in agreement with experimental measurements.

The losses in the reinforcing tapes and the binding wires can be calculated in a similar manner.

##### (6.2.3) Corrugated Strip Losses.

The losses in the corrugated strip can also be expressed as a fractional increase of conductor resistance by a factor  $\lambda_c$ , obtained by methods described in Section 6.2.2

$$\lambda_c = \frac{0.123\omega^2 \times 10^{-17}}{R_0 R_c} \quad (12)$$

$R_c$  = Resistance of corrugated strip, ohms/cm.

It should be noted that the value of  $R_c$  is related to the length of cable, and allowance should be made for the effect of corrugation increasing the resistance above that obtained with a flat strip.

##### (6.2.4) Ferrous Armour Losses.

Only three instances have occurred when ferrous armour has been used. As yet no formula has been developed to calculate these losses. Even with circular-shaped cables, such formulae as exist are not strictly accurate. In these instances the rating was determined after measurement of the losses in a trial length of cable.

#### (7) ELECTRICAL CHARACTERISTICS

##### (7.1) Power-Factor Temperature Curve

The variation between power factor and dielectric temperature between ambient temperature and 95°C was measured for voltages up to twice the working voltage of the cable. The independence of test results upon voltage showed complete absence of ionization in the cable dielectric, whilst the flat characteristic showed absence of any likelihood of thermal instability.

##### (7.2) Stability Tests

Stability tests have been carried out on a number of designs of cable, and the test results summarized below show that the dielectric was completely stable.

(a) 0.15 in<sup>2</sup> 66 kV cable.

Nearly 200 heat cycles were applied to this cable with a view to obtaining a breakdown. The test was discontinued owing to the limitations of the testing plant. All heating cycles were to a maximum conductor temperature of 95°C, and the number of cycles and the maximum a.c. stress at the conductor surface are given in Table 5. There was no significant change of power factor as measured hot and cold at the test stress.

**Table 5**  
STABILITY TEST ON 66 kV CABLE

Test voltage core-earth	Maximum a.c. stress	Number of cycles
kV	kV/cm	
57	129	98
63	143	10
68	154	10
76	172	16
83	187	10
89	202	10
95	215	10
101	230	10
107	243	10
114	258	10

Total number of loading cycles, 194

Results of other stability tests carried out in accordance with C.E.A. test requirements are given below:

- (b) 0.1 in<sup>2</sup> 33 kV cable; 20 heat cycles to 95°C at 95 kV/cm.
- (c) 0.3 in<sup>2</sup> 33 kV cable; 20 heat cycles to 95°C at 87 kV/cm.
- (d) 0.05 in<sup>2</sup> 66 kV cable; 20 heat cycles to 95°C at 148 kV/cm.
- (e) 0.4 in<sup>2</sup> 66 kV cable; 20 heat cycles to 95°C at 111 kV/cm.

### (7.3) Impulse Tests

Impulse tests carried out at the National Physical Laboratory on samples of 0.15 in<sup>2</sup> 66 kV cable, consisting of ten positive and ten negative impulses of 1/50 microsec waveshape at succeeding voltage levels until breakdown occurred, gave an average breakdown stress of 905 kV/cm.

Tests carried out on a sample which had been aged in the laboratory with loading cycles, with the conductor heated to a working temperature of 85°C, indicated that the cable would pass the standard C.E.A. 342 kV test at this temperature.

### (7.4) Tests with Direct Current

All the cables mentioned in Section 7.2, with the exception of the 0.15 in<sup>2</sup> 66 kV cable, were tested with direct voltages at the standard C.E.A. impulse-voltage test level for 15 min. No breakdown occurred.

### (7.5) Examination of Cables after Electrical Breakdown

Examinations made of cables after a.c. or impulse tests to breakdown showed that the breakdown path was a highly localized carbonized core between the central conductor and the point on the surface of the core which was in contact with the outer core. This corresponds to the region of maximum stress, since the dielectric is slightly compressed by the inward movement of the outer cores. This compression takes place during the first few load cycles, and is referred to in Section 8.1.

## (8) INSTALLATION

### (8.1) Land Installations

No special precautions are required for the direct laying of the cable in the ground. The procedure is straightforward as for

normal cables. The bending radii are the same as for single-core cables for bends about the major axis of the cable. Changes of direction are effected by gradual rotation of the cable about its longitudinal axis.

During jointing operations the cable section adjacent to the joints and sealing ends is frozen with solid carbon dioxide to prevent loss of oil from the cable and to permit independent vacuum and oil treatment of the joint.

When the installation has been completed the oil pressure is adjusted to a value above the minimum stipulated. When the cable is placed on load there will be a slight drop in pressure when the cable is cold, owing to a certain compression of the cable cores and to compression of the paper bedding between the reinforcing tapes and the lead sheath. After further load cycles there is no additional drop of oil pressure. When this occurs the oil pressure in the system is adjusted to a value consistent with load and seasonal ground temperature, and no further attention is required.

### (8.2) Submarine Installations

The design of the cable makes it admirable for submarine installations. The long manufacturing lengths made possible by the elimination of the laying-up operation reduce the number of joints in an installation considerably. A further point in favour of the cable is that the space required on the sea bed for laying and for pulling up is greatly reduced.

The increase of oil pressure owing to depth of laying is compensated by the hydrostatic head of water acting on the cable. With any large depths of laying, any pressure differences arising owing to the density of the oil being lower than the density of the water will not damage the cable. Let us consider a cable with an internal oil pressure of 40 lb/in<sup>2</sup> at sea level in the unloaded state. It is only below depths of about 900 ft that the oil pressure inside the cable is equal to the water pressure outside the cable. When a cable is laid to a greater depth, any tendency for the lead sheath to collapse inwards between the cores can be counteracted by the application of a thin bronze tape around the three cores under the lead sheath.

#### (8.2.1) Method of Laying (General).

The two methods employed for laying submarine flat pressure cable have already been described.<sup>6</sup> The first method is to lay the individual lengths of cable and then to joint the cable at sea. This method was used in 1951 with a 3½-mile installation of 3-core 0.3 in<sup>2</sup> 132 kV cable between Elsinore in Denmark and Hålsingborg in Sweden.

The second method of installation was to use a floating drum, as used for Pluto. A 3-core 0.25 in<sup>2</sup> 66 kV cable was laid in 1951 by this method across the Als Fjord in Denmark. In the summer of 1954 a further installation of 3-core 0.4 in<sup>2</sup> 132 kV cable was laid between Denmark and Sweden using this technique.

#### (8.2.2) Laying the 132 kV Øresund Cable by Pluto Technique.

It is interesting to give a few details of the Øresund 132 kV cable installation mentioned in Section 8.2.1. This was the first time that a cable for such a high working voltage had been laid by the Pluto technique. The length of the cable was 3½ miles and the conductor size was 0.4 in<sup>2</sup>. The magnitude of the step taken in choosing this particular installation technique can be seen when it is realized that the cable laid in 1951 was only designed for 66 kV; the conductor size was smaller, 0.25 in<sup>2</sup>, and the length of the previous installation was only 1½ miles.

The floating drum used for the Øresund cable had a diameter of 19½ ft, its total length was 36 ft, of which the cylindrical part was 33 ft. Only the central portion of 16½ ft was used for reeling of the cable, in order to improve the stability and sailing qualities

of the drum. The drum was stabilized by water ballast in the middle space, which is separated from the outer space by means of watertight bulkheads. A further refinement to secure correct ballasting is to subdivide the middle space into two equal spaces by means of a non-watertight bulkhead. The water ballast is taken in through a valve fixed at the hull near one of the watertight bulkheads, and this valve is opened by a hand-wheel. Near this wheel are two other valves through which air escapes when the drum is being filled with water, and these holes can also be utilized for emptying the drum by means of compressed air at 30 lb/in<sup>2</sup>. Alternatively, the emptying can be done by a syphon of 4 in diameter. A suitable braking system in the form of steel wire ropes laid into tracks at the end of the drum ensures that the cable is laid with suitable slack corresponding to the water depth at the time in question. The brakes can also be used for arresting the movement of the drum.

This floating drum was designed for carrying 250 tons of cable, as shown in Fig. 9.

be caused by some external mechanical force sufficiently great to cause a puncture of the dielectric and hence electrical breakdown of the cable.

In a submarine cable the oil will flow out of the cable owing to the internal pressure, and an oil spot will be formed on the water surface which can be observed by a boat or plane. An experiment carried out during the laying of the cable between Denmark and Sweden confirmed this. A diver released a container with 50 cm<sup>3</sup> of cable oil under the water. The oil film extended over a water surface of 100 yd and was readily located from a boat.

## (10) FUTURE DEVELOPMENTS

### (10.1) Cable Design

#### (10.1.1) Conductor.

Reduction of the overall dimensions of the cable should be possible by the application of compacted conductors. The



Fig. 9.—Floating drum for 132 kV Øresund cable.

### (9) OPERATIONAL EXPERIENCE

As a considerable quantity of this type of cable is in service it is worth while to review the experience to date.

The cable has been completely satisfactory as regards electrical performance, and no electrical failures of cable or accessories in service have occurred.

The mechanical performance also has been satisfactory. In 1949 one case of a split lead sheath associated with a manufacturing imperfection was found. Otherwise the cable has operated satisfactorily. The only cases of failure on accessories have been due to rupture of a wipe on two 66 kV stop joints.

#### (9.1) Oil-Leak Location

The location of oil leaks in land cables is made by freezing the dielectric solid with solid carbon dioxide, and observing the pressure in each of the sections. Further subdivision leads to the location of the leaking section by a process of elimination. The viscosity of the cable oil is such that a very large leak would have to occur to produce any likelihood of the cable having to be taken out of service. Indeed the size of the leak necessary would only

interstices between cable core and sheath should leave a path of sufficiently low hydraulic resistance to permit efficient impregnation of the cable during the manufacturing process.

An alternative solution is to use a hollow-core construction, which would permit more economical use of the copper for current rating. To offset any increase in expansion coefficient, owing to an increase in the oil content of the cable resulting from such a construction, the interstices between the cable core and sheath should be filled with oil-impregnated paper fillers. Experiments are being conducted to ascertain that a hollow-core conductor is not deformed by the mechanical work on the core when deformation of the cable sheath takes place during load cycles.

#### (10.1.2) Dielectric.

The discussion in Section 3 shows that any reduction in insulation thickness for cables for voltages above 33 kV is governed by the impulse-test requirements. The thickness of 33 kV cable dielectric is determined by mechanical considerations, and any reduction of dielectric thickness for these cables can only result from sustained field trials with the cable. The

improvement in impulse strength can be brought about by the use of conductor screening, impregnants with improved electrical characteristics or thin papers. Methods for conductor screening are now well established, with CB paper having preference, and any improved impregnant for the conventional oil-filled cable could be readily applied to the flat cable. The application of the thin-paper technique, with paper thicknesses of 1 mil, will undoubtedly come in the future, but the physical qualities of this type of paper require further investigation before the technique is generally accepted.

#### 10.1.3) Sheath.

Aluminium, which is now being used increasingly, could also be used in the flat-pressure-cable construction, and so possibly eliminate the reinforcement as with other types of supertension cable. After the core had been pulled into an aluminium tube, it could be died down on to the cable cores, and the corrugations formed in the same operation. A closer examination of the problem shows, however, that there are technical difficulties as well as manufacturing problems in achieving a solution. It has been found that, in order to provide sufficient stiffness to maintain the pressure variations between the limits of 7–90 lb/in<sup>2</sup>, the flat sides must be corrugated so that they are much stiffer for bending than the semi-circular ends of the cable. These limits of stiffness are independent of the sheathing material. Young's modulus for aluminium is  $10 \times 10^6$  lb/in<sup>2</sup>, as compared with  $18 \times 10^6$  lb/in<sup>2</sup> for 5% tin-bronze. Furthermore, the elastic limit of tin-bronze is 67 000 lb/in<sup>2</sup>, whilst the most suitable aluminium alloy, which contains 4% copper, has 0.1% proof stress of 45 000 lb/in<sup>2</sup>. For a cable using an aluminium sheath corrugated on the flat side to provide a pressure-controlling membrane, the thickness of the sheath would be at least three times the thickness of the bronze strip used on a lead-sheathed cable. This increase would mean that the stiffness for bending the aluminium-sheathed cable on to the drum would be increased five times. Likewise the stiffness of the semi-circular ends would also affect the behaviour of the membranous action of the cable.

The most suitable alloy would require special heat treatment to give it the high elastic performance required, and this allied to the already high cost of aluminium in tube form might render this cable design unsuitable from an economic standpoint. Considerable development work on aluminium alloy will be necessary before such a design will become a practical proposition.

#### (10.1.4) Reinforcement.

A reduction of the amount of material required to reinforce the cable, and a simplification of the design can be brought about by the use of a specially pre-corrugated bronze strip which is applied circumferentially to the cable. The strip would replace the circumferential tapes and corrugated strip, and development work has been commenced on this possibility.

#### (10.1.5) Other Designs of Cable.

At first sight it might be assumed that the design of a self-compensating cable would be simplified by use of a triangular construction. The flat sides would still be reinforced with corrugated strips, but would be specially designed to allow for the twist of the cores during laying up.

Examination of the working mechanism of such a cable demonstrates that the ratio of the permissible temperature rise between flat and triangular constructions is of the order 5 : 1, the maximum bending strain in the flat sides being the limiting criterion. This corresponds to a reduction of current rating of

2.3 : 1. The laying-up would also preclude the manufacture of long lengths of cable—a special feature of the flat design.

#### (10.1.6) D.C. Transmission.

Recently considerable attention has been refocused on the possibilities of the transmission of power by direct current for submarine-cable installations. The recent paper<sup>10</sup> discussed the likelihood of using direct current for this project. A serious objection to such a system was that the magnetic field of the proposed single-core cable installation would cause errors in the readings of the compasses of ships immediately over the cable. The use of a 2-core, or even 4-core cable, with "go" and "return" circuits would overcome this objection.

The use of a cable with a dielectric which is always fully impregnated is preferable for d.c. installations, as the distribution of electrical stress is governed by the resistance of the dielectric. The possibilities of flattening the cable under compressive forces experienced with cables laid at great depths has been dealt with in Section 8.2.

### (11) CONCLUSIONS

The successful operation of the 109 miles of flat pressure cable now in service indicates that the design of the cable is sound. Careful study of the cable design shows that improvement can be made to produce an even more economical design. The particular success of the cable with submarine installations makes it especially suitable for this field of application.

### (12) ACKNOWLEDGMENTS

Acknowledgments are due to the directors of Nordiske Kabel-og Traadfabriker, Denmark, and Enfield Cables Ltd., for permission to publish the paper, and especially to the authors' colleagues for carrying out the experimental work.

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[The discussion on the above paper will be found overleaf.]

## DISCUSSION BEFORE THE SUPPLY SECTION, 23RD NOVEMBER, 1955

**Mr. F. J. Lane:** This design of the cable makes it admirable for submarine installations since it is self-compensating, it takes up only a small space on the sea bed, and it has given very successful service. It is further stated that this cable can be manufactured in considerable lengths because the three cores do not have to be laid up.

The self-compensation feature is a great improvement in that no special oil-storage reservoirs are necessary along the route to cater for oil-volume variations due to changes in operating temperature.

The small space requirement on the sea bed is also certainly an advantage. If the cross-Channel connection is to comprise four single-core cables they must be laid some 600 yd apart, so that roughly a mile width of channel would be occupied. This wide band makes location of routes particularly difficult in the Channel.

However, if a three-core cable suffers any damage, the complete 3-phase circuit must be taken out of commission. The result of damage in terms both of loss of service use and cost of repair will be very considerably greater than would be experienced with a single-core cable.

There is also, of course, with a 3-core cable, the big problem of the 3-phase joints. These must be more complicated and take an appreciably longer time to make than the single-core joints, and the prospect of having to make a number of three-core joints during laying and on every occasion of a fault seemed a very serious objection when this type of cable was considered for the Channel.

I should be interested to know how long it takes to make a 3-phase joint at sea on a 132 kV cable of this type.

The successful service of the flat cable is noteworthy, but the cable lengths laid are no greater than 4 miles in any one case, so that the data are associated with limited lengths of cable in comparatively restricted sea ways.

I should like to have some idea as to 'the greater lengths' in manufacture which are mentioned. Judging by one reference in the film which was shown, the maximum length which can be manufactured is about two thousand yards, so that for a crossing like the Channel we would be faced with making 20–25 joints, and if those joints had to be made at sea, it would be an extremely difficult operation. Even if the cable were prepared on land for laying as a complete length, there would be the equivalent number of flexible joints, and however these joints are made, they are obviously weak spots as compared with a cable which can be made continuously without joints.

Cable laying by means of a drum is an interesting method of installing submarine cable. It naturally appeals to the engineer because it does not involve the twisting and untwisting associated with feeding a cable into the hold of a ship and feeding it out again in the laying process. On the other hand, it has some very severe limitations for a cable installation of any size. The drum would be required to carry a complete length of cable, and for the Channel crossing it would be some four times the size of the drum mentioned in the paper. I think the drum shown is capable of carrying 250 tons of cable, but the weight to be accommodated in a drum for the Channel crossing would be of the order of 1 250 tons. The costs of constructing a suitable drum would be £50 000–£60 000.

Clearly the control of the drum would be extremely difficult. Methods of controlling the cable feed-off are described, but the cable is by no means as flexible as one might think, and great care and accuracy in running off are important.

The navigation of a large drum on an accurate course across a waterway like the English Channel is a difficult proposition.

Accuracy is vital because one needs to be able to locate the cable precisely if trouble occurs at a later stage. It thus becomes extremely doubtful whether we could use a drum for a major installation on a busy sea-way which is liable to very variable tidal and weather conditions.

For short installations a cable of this design has many important advantages and it may be just right for the job, but for a major installation there seem to be many good reasons to doubt the suitability of the cable and laying method described in the paper.

There is a reference towards the end of the paper to the possibility of a 2-core d.c. cable. This will clearly not have the same ratio of width to depth as a 3-core cable, and I should like the views of the authors on the effectiveness of the self-compensation feature of a 2-core cable as compared with that of the 3-core cable.

Aluminium-alloy armour was initially chosen for the Channel cable studies, but it was finally rejected in favour of steel-wire armour. Aluminium wire is lighter—an important consideration with 3-core cable of this type—it gives lower sheath losses and has the necessary mechanical strength. However, steel has a much lower resistance to erosion in sea water, and its cheaper capital cost may more than offset the increased losses. Have the authors any experience of erosion of aluminium armour in sea-water conditions?

**Monsieur R. Tellier (France):** Électricité de France has been interested in the flat pressure cable for several years and has already carried out some experimental work at the Fontenay Research Centre which I should like to summarize briefly.

A first sample of cable was installed at Fontenay in 1951. It was designed for 66 kV with 95 mm<sup>2</sup> conductors. The limits of the working oil pressure of the cable were given as 2–6 kg/cm<sup>2</sup>. A sample of this cable, about 120 m long, including a 3-phase joint, was laid in the testing area in the shape of a closed loop and connected to the French 63 kV system. Daily heating cycles (8 hours' heating, 16 hours' cooling) were undertaken on this cable, especially to check its behaviour under repeated mechanical stresses.

So far it has withstood, without any trouble, 1 115 heating cycles, carried out with a current of about 275 amp. The only point worth mentioning is that, during summer conditions, some oil had to be removed from the cable to avoid the pressure getting too high. Consequently, in winter conditions the minimum pressure was below the limit given by the manufacturer.

However, I understand from the paper that the design of the cable has now been improved with regard to its mechanical behaviour, and that the design of the corrugated strip can be made in accordance with the operating conditions of the cable, in particular with a given maximum permissible temperature of the conductor. This seems rather important, since it shows that the cable can operate at the same maximum working temperature as the other types of cables and that it is not necessary to replace the conventional ammeter by a manometer in order to operate the cable in good conditions.

Another test was made on a short sample of the same cable about 1.4 m long, which was subjected to oil-pressure cycles between 4 and 6 kg/cm<sup>2</sup> by means of a mechanically-driven pump. The time duration of the cycle was about 3½ min. After 68 000 cycles, the pressure was adjusted so as to vary between 2 and 6 kg/cm<sup>2</sup>. The cable sheath failed longitudinally at both ends of its major axis after about 2 500 additional cycles. More recently two samples of a 132 kV flat pressure cable have been included in some of the dielectric tests which are being carried out on the French 225 kV system in connection with general investigation for the comparison of different types of submarine cables.

However, I would like to know what would be the variations of current-carrying capacity of a given type of cable, laid in certain conditions, for different soil temperatures, and for different static heads of pressure due to a slope, with the corresponding conductor temperatures. I think the curves would have the general shape shown in Fig. A.

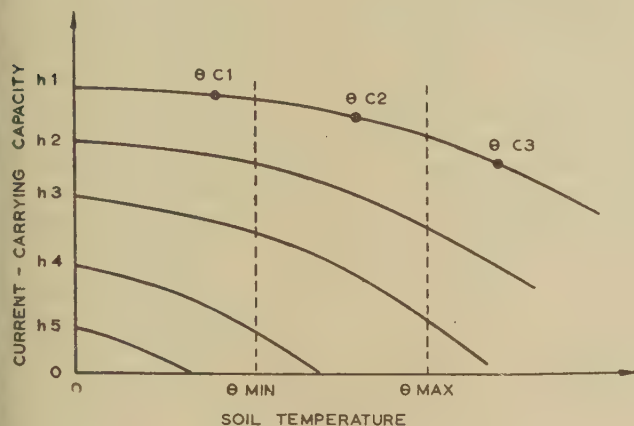


Fig. A

Finally, I should like to know whether the possible overloading of the cable is governed by pressure conditions or by temperature conditions of the conductor. This question seems to be related to the knowledge of the time-constants of the pressure curve and of the copper-temperature curve of the cable.

**Dr. A. N. Arman:** For level, or nearly level, routes there can be no doubt that the flat cable has proved entirely satisfactory. Unfortunately these are seldom encountered in this country. Static heads of up to 100 ft are quite common, and in such cases the pressure range available for accommodating thermal expansion of the oil is reduced to about 25 lb/in<sup>2</sup>. Could the authors make a clear statement as to the pressure range actually required to allow for conductor temperature cycles up to 85°C?

If grading of the corrugated strip is adopted, some longitudinal movements of oil will have to take place, and then cooling transients (with oil of higher viscosity than oil-filled cables) would have to be allowed for. The authors have dismissed this matter too lightly, and much more information is essential if a proper assessment of the cable is to be made.

Illustrations of the sealing ends would have been interesting. Could the authors state whether the condenser-type terminations, mentioned as being used in the 220 kV sealing ends, have passed the appropriate impulse test, which, for the I.E.C. Specification, is of 800 kV peak?

Taking gradient limitations into account, stop joints must surely often be necessary. Joints of the type normally used for oil-filled cables have been used in connection with flat cables, but have any designs been developed specially for use with them?

The interstices between cores, although quite adequate to allow for any small longitudinal oil transference, must offer considerable resistance to moisture and gas removal in the final drying and impregnating process. What maximum drum lengths of 33 kV and 132 kV cable have actually been made?

The authors have commented that little has been published on the current rating of flat cables. Although much interesting information is given on the methods of calculating the cable movements, pressures, etc., there is still no real information in the paper on the practical results of such calculations or of experimental confirmation of ratings so calculated. What actually are these current ratings? Are they above or below those of the corresponding sizes of gas-pressure and oil-filled

cables, and by how much? It is appreciated that the ratings must vary according to the route profile, and this may make it difficult to present a simple Table of carrying capacities. Ratings for level routes could, however, be stated, together with the reductions which must be made for various static heads. This latter point is of the greatest importance.

Can the authors give any actual test data regarding the impulse strength at a conductor temperature of 85 or 90°C? The figure of 905 kV/cm was evidently obtained at ambient temperature, and the viscosity characteristic of the oil indicates a probable reduction in strength at the higher temperature. From the point of view of the cable designer the breakdown strength is of great interest, since it enables an estimate to be made of the ultimate economies in design which can be made as compared with other types for which breakdown strengths at elevated temperatures are already known.

**Mr. D. B. Irving:** Two main advantages appear to be claimed for this notable development. One is the longer manufacturing lengths, to which reference has already been made, and the other is that the advantages associated with pressure in the dielectric are obtained without the necessity for external accessories.

The authors state that the paper tapes on the conductors are applied 'in the usual manner.' To my knowledge there are several manners in which tapes are applied to conductors, and it would be interesting to know which is used. They state that the cable was satisfactorily impulse tested with a standard 1/50 microsec waveshape. I wonder whether that wave was actually achieved in the tests and whether the word 'standard' should be altered to 'nominal.'

I note the authors' conclusion from the accelerated life tests on the sheath that the sheath life in service would be at least 40 years. Was that sheer coincidence, since 40 years is the financial life normally allocated to cables?

When the prospective user is confronted with a new type of cable, two questions arise. First, is the cable as reliable as other contemporary types? Secondly, will it cost less to make and install? The paper has given an answer to the first question, but it gives no answer to the second one. Perhaps the authors might give some information on that aspect, if not in terms of money at least in terms of weights of material used for a given service as compared with corresponding cables of other types.

Électricité de France are to be congratulated on having had an experimental cable of this type at their testing station since 1951. I must confess to a feeling of disappointment that the authors were unable to include a similar note about this country in the paper.

**Mr. C. C. Barnes:** In this country we have five proved systems of super-high-voltage cables, as shown in Fig. B, and therefore it is necessary to consider carefully the case for the flat pressure cable in relation to the existing well-established cable systems.

Two of the authors are closely associated with the external gas-pressure system, known as the compression cable, and it is regrettable that they have failed in the paper to make a critical technical and economic comparison with this system.

From Section 2.2.1, CB screening appears preferable to metallized-paper-tape screening from production considerations, but would the authors state the factors which determine their acceptance of paper tapes loaded with colloidal carbon? Furthermore, how can one be sure that such tapes will maintain fully their screening properties throughout the long life anticipated for the cable system?

Section 2.2.6 deals exceedingly briefly with anti-corrosion protection. The life of any pressure cable is dependent on the anti-corrosion protection adopted, and a detailed summary of the authors' tests on the covering specified would be of considerable interest.

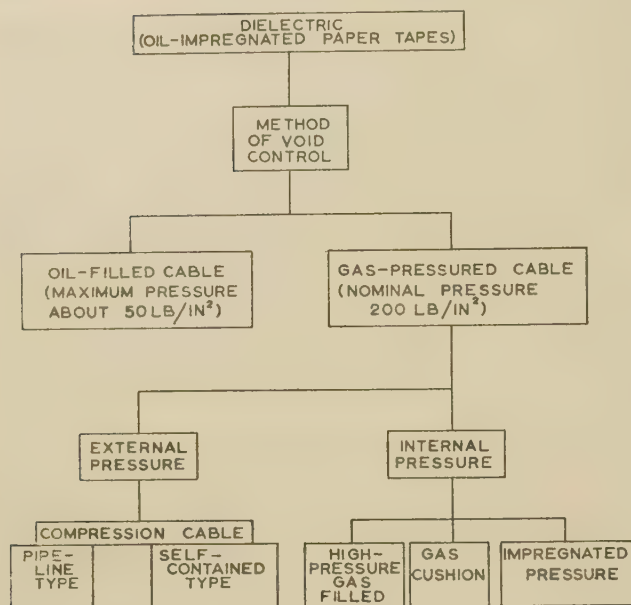


Fig. B.—Classification of British super-high-voltage power cables.

I am very pleased to note the statement in Section 3.1 '... lower-voltage cables, where the mechanical characteristics of the thin dielectric determine the design stress.' This is most important, as very-thin-wall 33 kV pressure cables can only show a small economy over designs with, say, 0.13 in radial thickness of dielectric, and this economic saving is immediately lost if the handling and/or laying operations damage the very thin dielectric in any way and result in service failure.

The following passage also occurs in Section 3.1: 'Generally it can be stated that the variation in impulse strength of all the cables mentioned in Section 1.1 of the paper is not more than 10–20%.' These are wide limits, and comparative factual test data on this point would be of considerable interest.

In particular, a summary of all the impulse tests on the flat pressure cable should be included in the paper, with a statement on the spread of results. I assume that the impulse tests which have been made were with the conductor at its maximum rated temperature.

Section 3.2.1 refers to a flat pressure cable for service with unscreened conductors designed at 100 kV/cm. What type tests and breakdown levels were obtained during the authors' tests to justify the adoption of such a high design stress? I assume that the cable joints and terminations were also fully tested.

Section 6 deals with current ratings. The data given appear to correspond to Reference 9 of the paper. It would have been helpful, however, if tabulated current ratings for the flat cable had been given, so that more detailed comparisons could be made with other systems.

In view of the relatively large surface area of the flat pressure cable, I would appreciate an amplification of the authors' statement that the current ratings are similar to other designs of 3-core super-high-voltage cable.

What difference of level is accepted before the use of a stop joint, as mentioned in Section 5.2.1, is recommended?

How many loading cycles were applied, and what was the average breakdown stress of the aged sample mentioned in Section 7?

**Mr. J. Banks:** A new design of cable satisfying the theoretically ideal solution of being self-contained is always attractive. However, it must be economical and thus must utilize copper more or

less to the same extent as present standard designs. This must involve the use of conductor temperatures of the order of 85°C. There is little doubt that for land use the need to operate at such high temperatures is the prime feature on which this design of cable is to be judged.

For instance, in Section 2.2.3 reference is made to the life of the lead sheath under load-cycle conditions, and a test is reported in which the cyclic strain effect was reproduced by pumping oil into and out of a sample. I would like to know whether the sample concerned was a complete cable, i.e. whether the recovery of the sheath was merely that provided by the reinforcement, and I would also like to know the temperature of the sheath during the test. Obviously the fatigue behaviour of the sheath at elevated temperatures would be a crucial issue, and I would have thought that an extended test with heat cycles produced by conductor loading must be the basis of judgment. It is just possible, of course, that some of the more recent installations listed in Table 1 are operating on a design temperature of 85°C, and so I wonder whether the authors could give some information on this. Incidentally the value of Table 1 would be greatly enhanced by a column giving the electrical design stresses used.

In Section 3.3.1 it is stated that half the total expansion is accommodated by the deflection of the flat side. I do not know whether this should be interpreted to mean that each flat side contributes half the oil accommodation or, as seems more likely from the context, that half the accommodation comes from other than flexing of the flat sides. If the latter interpretation is assumed, I would ask for more evidence to justify the assumption that the whole of the accommodation is truly elastic. Furthermore, in Section 3.3.3, the design would seem to assume that the accommodation characteristic is linear with pressure.

**Mr. C. H. Gosling:** I should like to confine my contribution mainly to the use of this type of cable in London, where we have recently been installing 132 kV cable lengths of between 500 and 600 yd in the centre of the city. This is facilitated by installing ducts across main roads prior to the pulling operations. It would be interesting to learn the possibility of pulling this cable through ducts and the rating to be expected under such conditions. There might be some difficulty if there are bends in the duct crossings.

With regard to sealing-end structures, I notice that the authors previously raised the structures after making the sealing ends. Space in London would be so limited that this would not normally be possible.

I am rather disturbed by the life of the sheath as stated by the authors. We have certain cables which have three load cycles per day, and this would amount to some thousand cycles during the year. I am gratified when the French state that they find that a factor of three or four times the number of load cycles applies to the life of the sheath as compared with the authors' figure.

It would seem that the magnitude of the transient conditions which can exist in the sealing ends is dependent upon the length of the tails. If there are very long tails such as we experience—sometimes 30–40 yd—the restriction would affect the transient conditions in the sealing ends. I feel that transposition would be essential so far as we are concerned. If the cores are not transposed we are likely to find induced voltages in the pilot cables of an order which would not be acceptable and serious interference with local telephone circuits would result.

It is suggested that we should have an alarm at the trifurcating joint only, but this leads to two points, bearing in mind the high viscosity of the oil:

(i) Are we quite satisfied that a leak occurring in the centre of the hydraulic section will give an alarm prior to any electrical deterioration, particularly during off-peak conditions?

(ii) Possibility (i) would seem to dictate that a flow test is necessary prior to the cable being put into commission in order to ensure that there is a clear passage for oil flow. I suggest that a further flow test should also be carried out after the initial loading cycles when compression has taken place.

From the curves given, it can be seen that there is a  $5^{\circ}\text{C}$  difference in temperature between the centre and outer conductors when the centre conductor is operating at  $80^{\circ}\text{C}$ . While this does not appear to be very great, it would seem that if we could equalize these temperatures we should save between 4 and 5% of the total copper in the cable. This could be achieved in one of three ways. The first is by increasing the core screen thickness to dissipate more heat from the centre conductor. The second is to compact the centre conductor only, in order to reduce its resistance compared with the outer cores. Finally, in the hollow-core construction we could employ larger ducts for the outer conductors as compared with the centre one.

**Mr. E. E. Hutchings:** The authors have referred in Section 6 to three methods of determining the internal thermal resistance of the cable. Each depends on measurement made on cable models, and each has been applied in the past to conventional cable designs with complete success. But the problem in the case of those conventional designs is perhaps more simple than with the flat cable, since one can assume that the three conductors are operating at the same temperature, and that the heat-flow diagram is symmetrical about each of these conductors and identical in each of the three  $120^{\circ}$  sectors of the cable.

Neither of these statements is true with the flat cable, and I should be interested to know whether the models used were those of the complete cables and whether any difference had to be made in the potential applied to the three electrodes representing the conductors. From Fig. 8 the difference in temperature does not appear to be very great. I should like to know how that Figure was obtained, i.e. whether it represents actual cable tests or whether it was obtained in some other way.

With regard to the model test, has the thermal effect of the aluminium-foil screening been taken into account? This can be significant in a 3-core cable.

I wonder whether the statement that current ratings are similar to those of other designs is intended to be quantitative. Section 10.1.5 refers to a reduction of current rating of 2.3 : 1 between the flat cable and a triangular cable with flat sides. This appears to be based on mechanical criteria, and it is not clear whether it relates to the same temperature rise in the two cases.

The difference in the impedance of the three conductors due to their flat spacing is mentioned in the paper. Generally speaking, three conductors in flat spacing differ as regards the resistive component by an amount which is comparable with the resistance of the sizes of conductor dealt with in this paper and as regards reactance by an amount which is about half that order. With submarine cables, where the joints are not transposed and where one may have lengths of three miles or so, the unbalance may be significant. Do any operational effects result from this?

Finally, has any deformation been observed in flat cable, resulting from heavy fault currents? Any initial deformation could lead to complete collapse under heavy electromagnetic forces, since both the internal periphery of the sheath and the volume within that periphery are for a triangular configuration of conductors considerably less than for a flat configuration of the same conductors.

**Mr. D. T. Hollingsworth:** In assessing the qualities of the cable it is necessary to examine closely the economic aspects, particularly as from a technical point of view it does not offer advantages over other types of cable for either land or sea use. The material content of this cable must be greater than that of a circular cable,

particularly as regards the weight of lead required for the sheath. As the material content in the overall cost of super-high-voltage cables is approximately 70%, it is a very important aspect, and this type of cable will therefore be uneconomic in comparison with existing types. Furthermore, it does not appear to possess technical advantages.

An undesirable feature is the twisting which might occur during laying. If the cable were laid across the English Channel, for instance, considerable tensions would be experienced in the armour, which must produce a torque in the cable. Accordingly the cable would twist as it passed over the bow sheaves of the cable-laying ship. I would expect that this twist which is left in the cable would interfere with the compensating reinforcement around the lead sheath. Would the authors give data relative to tension caused by twisting of the cable, and would it interfere with the action of the sheath reinforcement?

**Mr. G. S. Buckingham:** Owing to the high charges for the supply and installation of joints, I feel that the long lengths which can be made might make this cable economical for land cables.

The second advantage of the cable is that it needs no compensating oil tanks, and that will also result in a saving in built-up areas such as occur in the Midlands. In such areas the cost of excavation and the number of underground obstructions is very great, and it is to be hoped that these savings may influence distribution engineers to give special consideration to this cable.

However, if the working pressure of a normal orthodox oil-filled cable were raised to  $100\text{lb/in}^2$ , we might be able to dispense with the compensating oil tanks without resorting to this new design. Could the authors state whether such a cable could be designed to dispense with compensating oil tanks along its route, limiting them to the terminations, where accommodation is usually easier.

This flat cable must stand or fall on the total cost of each installation, particularly as there are some inherent disadvantages. I think distribution engineers will be apprehensive of a flat cable which turns over on itself when it is being laid, and they may also be wary of the new principle of self-compensation and the high pressure. One of the disadvantages of the gas-pressure cable is that it operates at  $200\text{lb/in}^2$ . Distribution engineers are likely to seek some considerable financial incentive before wholeheartedly acclaiming this type of cable.

**Mr. N. Klein (Israel: communicated):** The largest permissible temperature rise of this cable has not previously been known sufficiently well. Apart from the usual factors, the temperature limit of this cable depends also upon special mechanical properties. The authors indicate that no fatigue failure of the lead sheath need be feared up to the highest temperatures in use with customary pressure cables. It would be interesting to learn how the strains in the lead sheath were measured on fatigue tests and what were typical strain distributions round the periphery at varying temperatures.

It also appears that no inelastic deformations are expected in the corrugated strip and in the binding wires. How was the increase in cable volume, due to bending of the corrugated strip only, separated from the rest in the measurements in Fig. 2?

A further factor for the temperature limit seems to be the plastic-core compression in the direction of the long axis. What is the magnitude of the permissible compression and how does it depend upon the insulating paper? Were relations between temperature rise and core deformation established similarly to the case of the corrugated strip?

Developments of pressure cables, in which lead sheath and reinforcements are replaced by an aluminium or other alloy sheath, would be very interesting. Such a sheath could be deformed only elastically on operation. The results of our recent

analytical investigation on the properties of pressure cables with elastically operating sheaths are summed up as follows:

Elastic-sheath pressure cables could work up to the highest temperatures found with customary pressure cables, without the need of corrugations in the flat sides. The largest permissible conductor temperature rise in elastic-sheath pressure cables is proportional to  $(\epsilon_{max}^3 E / p_{min})^{1/2}$ , where  $\epsilon_{max}$  is the elastic-strain limit,  $E$  is Young's modulus of the sheath material, and  $p_{min}$  the minimum permissible pressure in the cable. If  $p_{min} = 7 \text{ lb/in}^2$ , permissible temperature rises of the customary pressure cables are obtained with elastic-sheath flat cables when  $\epsilon_{max} = 0.0015$ . This corresponds, in the case of aluminium-alloy sheaths, to an elastic-stress limit of about  $15000 \text{ lb/in}^2$ , which can be obtained

with several alloys already in the semi-hard state. Details of analysis show that, for equal pressures, the largest permissible temperature rise of triangular cables is about half that of flat cables. Customary temperature rises should be obtained with triangular elastic-sheath cables when the strain limit of the sheath metal is round  $0.0022$ . Even this can be obtained with many alloys, which are not necessarily those of aluminium. Cables are in use with non-magnetic steel sheaths, and highest elastic-strain limits of  $0.006$  are known. Flat and, especially, triangular, elastic-sheath pressure cables are very simple, and although difficulties connected with their transport are obvious, it appears well worth while to investigate their practical possibilities.

#### NORTH-WESTERN SUPPLY GROUP, AT MANCHESTER, 10TH JANUARY, 1956

**Mr. E. L. Davey:** I want first to deal with the economic position of this cable in relation to other types of high-voltage cables already on the British home market. The authors have been using a stress of  $85 \text{ kV/cm}$  for  $132 \text{ kV}$  cables, and they state that stresses of  $100 \text{ kV/cm}$  can be used. Therefore, on this basis, the cable is on an equal footing with existing high-voltage cables.

With regard to the current rating, however, the cable seems to be below the level of other cables. Fig. 8 shows that, for a  $66 \text{ kV}$  3-core cable, the centre core runs at a temperature  $5^\circ \text{C}$  hotter than the outer cores, and for a  $132 \text{ kV}$  cable this difference must be greater. The current rating must therefore be less than for normal 3-core cables, resulting in a larger conductor size for a given rating.

In the construction and manufacture of the cable, there is at least  $5\%$  more lead as compared with the normal 3-core round-conductor cable; this is a very heavy economic disadvantage at the present high price of lead.

The amount of non-ferrous metal, presumably bronze, contained in the reinforcement construction is very high. First, there is a layer of metallic tapes over the sheath, followed by corrugated strips bound down by non-ferrous wires, and since the cable perimeter is  $5\%$  in excess of that of the round cable, the cost of these materials will be very important in the economic assessment of the cable. My own attitude towards this cable, based on the information contained in the paper, is that I would not contemplate its manufacture for land use, because the economic aspect weighs so heavily against it as compared with existing types of cable. For submarine use the economic drawbacks may be compensated for by other useful properties, but the main drawback appears to be that the cable cannot be made in continuous lengths. The use of flexible or factory joints, which really consist of reconstituting the cable in order to provide long submarine lengths, has the drawback that production of the cable is delayed since these joints necessarily take a long time to make.

In Section 2.2.3 the authors provide certain data on the life of the cable under cold working. The figure of 13000 cycles minimum sheath life applies to tests carried out at ambient temperature; in order to obtain the service life, when the sheath is at a high temperature, their figure should be roughly halved to a value of 7000 cycles. With 300 cycles per year this means that the sheath life in service would be at least 20 years. I should also like the authors to elaborate their point regarding the quality of the lead sheath. Reference is made in this Section to the production of an oxide-free sheath on a ram-type press, while later, reference is made to improvements in manufacture which have produced a sheath in which the welds have mechanical properties equivalent to the rest of the sheath. Have the authors used as a criterion for this statement very-long-time creep tests carried out over a period of several years? This is agreed by

metallurgical experts to be the correct criterion for the quality of a cable sheath.

In Section 2.2.5 it is stated that, for large submarine cables aluminium-alloy wires are used. The primary use of armour wires on submarine cables is to prevent abrasion where the cables rub on rocks, and from this aspect the abrasion resistance of steel is roughly six times that of aluminium alloy quoted in the paper when the abrasion tests are carried out under sea water. The use of steel armour instead of aluminium on single-core submarine cables necessitates a small increase in conductor size to maintain the rating, and it involves higher losses, but the capital cost of the steel armour is lower as compared with the aluminium armour. This saving in capital cost outweighs the capitalized cost of the extra losses, and hence there is an absolutely clear case for the use of steel armour on submarine cables on economic and technical grounds. With the authors' cable which approaches the normal 3-core type the case should be even stronger as regards the economic side, since the losses are lower. Have the authors any difficulty in applying steel armour to the flat cable?

With regard to d.c. cable, I agree with the authors that the use of 2-core cable with 'go' and 'return' circuits would eliminate the external magnetic field associated with single-core d.c. cables. This does emphasize the desirability of being able to lay up and manufacture multicore cables for submarine use in continuous long lengths.

**Mr. D. H. Booth:** In Section 2.2.1 the authors quote a figure of  $20\%$  for the improvement of impulse and a.c. strength owing to the use of carbon-paper screening. This is contrary to the data given in the recent paper by Priaroggia and Palandri,\* where experimental results are given to show that carbon-paper screening, although of great advantage to improvement of a.c. performance, gives only a small improvement—not greater than  $5\%$ —under impulse conditions. I would be grateful, therefore, for further information from the authors in support of their claim.

In Section 2.2.2 it is difficult to understand why the authors recommend such a high oil viscosity at  $20^\circ \text{C}$ . Admittedly they say it permits jointing with little modification to normal solid-type practice, but in Section 8.1 they describe the prevention of oil flow by freezing and the filling of the joint by vacuum impregnation methods. In other words, they give a description of a procedure similar to conventional oil-filled cable-jointing practices.

From eqn. (5) it may be seen that the maximum permissible height of the corrugated section is designed on the basis of the limit of proportionality. It would have been better if this had been expressed as a  $0.01\%$  proof stress, and one would have expected some factor of safety, say 2, to be used, as in the standard calculation on pressure-cable reinforcement design.

\* PRIAROGGIA, P. G., and PALANDRI, G.: 'Research on the Electric Breakdown of fully Impregnated Paper Insulation for High-Voltage Cables,' American I.E.E. Paper No. 55-692.

In Section 7.2 it is noted that the maximum conductor size type-tested at present is 0.4 in<sup>2</sup>, and that this is also the maximum size so far installed. Do the authors feel that this is the limiting maximum conductor size for this form of cable, and if so, what are the reasons for this serious limitation?

**Mr. H. G. Allen:** In Section 9.1 the authors state that leaking oil in a submarine installation will be evident on the water surface. There is no guarantee, however, that, if the anti-corrosion serving is sound, the oil will emerge at the point where the lead sheath is punctured. Have the authors further information on this point?

**Mr. J. B. Kilshaw:** A round-type cable can easily suffer damage, and 'bird-caging' of the armouring can occur owing to mishandling. What experience has been gained on the vulnerability of the flat pressure cable for use in land situations? It would have to be twisted on to its major axis when rounding corners and bends, and difficulty would apparently be experienced in preventing the cable from twisting.

**Mr. W. Y. Murray:** According to Fig. 5, the corrugated tube which encloses the jointed cores fits snugly on the cable sheath at each end and is joined to it by plumbing. For the particular cable of Table 4, where the conductor stress is about 95 kV/cm at working voltage, if the stress at the ferrule in the joint is limited to 75% of the conductor stress in the cable (see Section 5.1), the overall dimensions of the built-up cores at the centre of the joint exceed those of the lead-covered cable by about  $\frac{1}{3}$  in along the

minor axis and by as much as  $1\frac{1}{2}$  in along the major axis. Does this mean that the flexible tube is in two halves which are later joined together at the centre, or is the stress limit increased above 75%?

I should have appreciated a more detailed description of the submarine rigid joint. If the joint shown in Fig. 6 is symmetrical about its centre, I can visualize certain difficulties of construction. For example, how is the inner phosphor-bronze tube passed back along the cable during jointing? Also, the design shown seems to be essentially a shallow-water joint. To what depths has this joint been laid, and how long has it been in operation? Even at quite moderate depths, as a rigid joint of this type is being lowered to the sea bed, there is an appreciable weight of suspended cable, and I should have thought that measures other than merely doubling back the armour wires would have been necessary to prevent severe bending of the cable at each end of the joint.

Finally, it seems to me that serious corrosion risks are incurred with this design of joint. For example, the spun copper gland which encloses the armour clamp is cathodic to both the aluminium-alloy armour and the steel casing, and I suspect that severe galvanic corrosion of these would occur. A similar risk exists internally at the plumbed joints, which can be reached by sea water from capillary action along the servings or by cavitation of the compound owing to temperature changes.

### THE AUTHORS' REPLY TO THE ABOVE DISCUSSIONS

**Messrs. J. S. Møllerhøj, A. M. Morgan and C. T. W. Sutton (in reply):** Several speakers have asked for further details of the lead-sheath fatigue tests: these were made on actual cable samples and a full report will be given elsewhere.\* The contribution by M. Tellier is very important, since it brings evidence from an independent authority supporting our findings. The tests undertaken by Électricité de France were made on cable of early manufacture, and so with the greater precision of design now possible modern flat cables will give even better performance. When considering the effects of three load cycles per day, Mr. Gosling must realize that the thermal constants of a buried cable will not permit the same variation of cable temperature as with daily loading cycles. This means that the magnitude of the cyclic strain will be correspondingly reduced, and the number of cycles to failure will be increased. We would also mention that the minimum life of 40 years for the sheath allowed for the temperature of the sheath being higher than that of the test samples.

The economic question has received considerable attention, although the main purpose in presenting the paper was to assess the cable technically. We feel that economic comparisons between different designs can be judged in a fair manner only when each of the cables is applied to an actual installation. As Mr. Barnes points out, there are five proved systems of super-high-voltage cables in Great Britain, and we must necessarily assume that the choice of a particular type for a particular installation is made from economic considerations. There are many variables to be taken into consideration—voltage, load, length of circuit, pulling lengths, gradients on route, conditions of installation, etc.—and we are sure that instances will arise in which the flat pressure cable will prove to be the most economic. A quick examination will show that the flat pressure cable uses the same materials as other designs, and although it is correct that there is a small increase in the amounts of lead and reinforcing materials, these factors are offset by the elimination of tanks, simplicity of jointing and the use of longer lengths.

The question of current rating has also been raised by several contributors, and it would seem that some confusion has resulted because these speakers have incorrectly assumed the same conditions as with conventional 3-core cables. Actually, the internal thermal resistance of the central core, which is tangential with the lead sheath, is of the same order as that of one core of a conventional 3-core cable, and so the values for the outside cores are lower. The flat shape of the cable also improves the heat dissipation, resulting in the external thermal resistance being less than for a conventional 3-core cable. The lower temperature of the outer cores means that the losses in these are slightly reduced. The current rating of the flat cable is little different from oil-filled cables of 3-core construction for the voltage range 33–132 kV. Fig. C gives the ratings and weights for 33 kV land cables.

The tests reported in Section 6 were made on models which

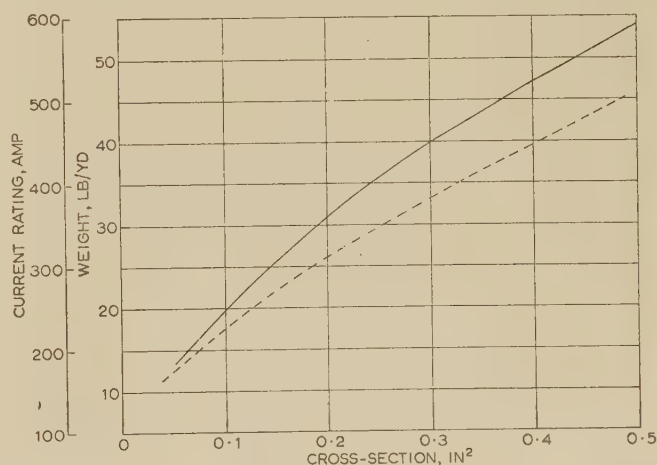


Fig. C.—Current rating and weight of 33 kV flat-type underground cable.

— Maximum continuous current capacity:  $G_R = 120^\circ \text{C/watt/cm}^2$ ;  $\theta_t = 70^\circ \text{C}$ .  
 --- Weight.

\* KJAER, H.: 'On the Fatigue Problem in Cable Sheaths', C.I.G.R.E., Paris, 1956, Paper No. 208.

represented a quadrant of the cable, and the asymmetry of heat flow was accounted for; early tests were made with the central-conductor model at a higher potential than the outer. Results indicated that there was little difference when all cores of the model were run at the same potential. The effect of the aluminium-foil screening has been taken into account, as suggested by Mr. Hutchings, and the possibility of improving the rating by increase of screen conductivity, as suggested by Mr. Gosling, has been considered.

The other suggestions made by Mr. Gosling have also been considered. The compacting of the central core would mean making a joint between conductors of unequal diameter when transposing conductors at joints. The idea of using ducts of unequal diameters also means that the wires on the outer conductors would be 2% smaller than those of the central conductor, and this would mean departing from standard sizes.

In reply to Messrs. Banks and Klein, the two parts of the increase in cable volume were obtained by measurement of the change of shape of the cable and of the curvature of the corrugated beam. Both components were found to be elastic, and, as stated in the paper, there is a compression of the cores in the direction of the long axis. The type of paper used and the tightness of the paper lapping are governed by electrical considerations, so that a magnitude of maximum permissible compression does not arise.

The limitation of the permissible temperature rise of the triangular design is that of the mechanical properties of the reinforcement. We would point out to Mr. Klein that  $\epsilon_{max}$  for 5% tin-bronze is 0.0037, which is double that for aluminium. Designs using steel sheaths have been tried, but these are far too stiff for taking off onto a cable drum and would complicate jointing. In reply to M. Tellier, the overloading of the cable is governed by temperature and not by pressure conditions in the cable.

The 2-core d.c. cable will have adequate self-compensating properties despite the change of depth/width ratio, as compared with the 3-core cable. There is a reduction of oil volume in the cable for the 2-core design, and because of the dependence of electric stress upon temperature gradient for d.c. cables, the maximum working temperature would be reduced.

The design of cable to be used when severe gradients are encountered is best determined from a detailed consideration of the route. Generally, we would not consider stop joints to be necessary unless there was a static head in excess of 100 ft. Any slight cooling transient associated with gradings of the corrugated strip will occur in regions where the pressure is greater than the minimum permissible, and so does not give rise to unduly low pressures.

The impulse strength of the cable has been referred to by several contributors. The tests on aged samples were made on 66 kV cable which had been subjected to 194 loading cycles, and were made with a maximum conductor temperature of 85°C. Joints and terminations were included in test lengths. Whilst breakdown figures are important to the designer, the withstand voltage is of greatest importance to the user.

The improvement of 20% for impulse and a.c. strength owing to the use of CB screening has been based on experience with compression cable.\* The quality of the paper varies considerably with place of manufacture, and we are aware of the difficulties associated with some grades.

With regard to manufacturing details, 2000 yd lengths of 132 kV 0.4 in<sup>2</sup> cable have been manufactured, and conductor cross-sections up to 0.4 in<sup>2</sup> have been made and have been type-tested. The design of the corrugated strip has been based on the elastic limit, since it is the elastic behaviour which is being calculated. Design on proof stress for a membranous cable is not sound practice, although this is convenient for designing the reinforcement for a cable where the pressure is static.

The use of the cable for submarine installations has been dealt with by Mr. Lane. Comparison with single-core cable should be on the basis of two flat cables against four single-core cables; there is little difference in cost between these two alternatives. The likelihood of the two flat cables being out of service simultaneously is less possible than that for two of the four single-core cables, and so the flat-cable system would be more reliable. In our opinion there would be little difference in repair costs for a joint in either type. When considering submarine-cable installations we regard the provision for the making of flexible or rigid joints as being inevitable with all types of cable; so if one joint can be made satisfactorily there is no reason why 20-25 should not also be made, especially under land conditions and wound onto a drum. The drum necessary to lay a 3-core 132 kV cable across the Channel would be of the same size as that used for laying the H.A.I.S. cable in the Second World War.

The alternate use of aluminium-alloy or steel-wire armour for submarine-cable installations is common to any design. Submarine cables of the flat pressure type have been installed successfully with both types of armour, and no difficulty is experienced in applying the armour. The problem of twisting of the cable during laying is known to the authors, who have not experienced any difficulties with cables laid from a floating drum or from a drum mounted in a ship.

With regard to the possibility of increasing the maximum working pressure of oil-filled cables suggested by Mr. Buckingham, and accommodating the expansion of oil in tanks located only at the terminations, we would point out that the oil-pressure cable of M. Domenach operates with a pressure of 200 lb/in<sup>2</sup>, but the oil expansion accommodation problem is no different from that of a normal low-pressure oil-filled cable; oil tanks are necessary.

In conclusion, it should be emphasized that both land and submarine 3-core flat pressure cables up to 132 kV have been successfully laid and jointed, and are giving excellent performance in service. The advantages of this design will be better assessed in Great Britain when more engineers have installed and used flat pressure cable. Then doubts as to the suitability of the construction for pulling into ducts, bending, etc., will be removed.

\* SUTTON, C. T. W., and MORGAN, A. M.: 'Review of Compression Cable Experience', C.I.G.R.E., Paris 1956, Paper No. 204.

# THE MEASUREMENT OF STEAM TEMPERATURES IN POWER STATIONS

By D. H. LUCAS, B.A., Associate Member, and M. E. PEPLOW, B.Sc., Graduate.

The paper was first received 14th December, 1954, and in revised form 29th March, 1955. It was published in June, 1955, and was read before the SUPPLY SECTION 14th December, 1955.)

## SUMMARY

Errors in steam-temperature measurement may occur because of:

- Incomplete mixing of steam from different sources.
- Variation in temperature over the cross-section of a steam pipe.
- Difference in temperature between steam and thermometer pocket.
- Difference in temperature between pocket and thermometer.
- Slow response of the thermometer.
- Inaccuracy in the measuring instrument.

These sources of error have been considered theoretically and the results are given in a form suitable for application to practical cases. It is considered that two streams of steam entering a single pipe are thoroughly mixed in a distance of about 30 diameters. The temperature gradients in high-velocity steam in a well-lagged pipe are negligible except near the pipe wall. Errors of steam-temperature measurement using pockets can be made negligible by proper design, with the exception of errors due to time lag. Thermometers immersed in the steam have greatly reduced errors due to time lag. Confirmatory experimental work is described.

## LIST OF SYMBOLS

- $a$  = Cross-sectional area of pocket or thermometer stem.  
 $C$  = Concentration in units of quantity per unit volume.  
 $c$  = Heat capacity of thermometer per unit length.  
 $c_p$  = Specific heat at constant pressure.  
 $D$  = Diameter of pipe.  
 $d$  = Diameter of pocket or thermometer stem.  
 $f$  = Dimensionless number in the expression for the flow-resistance of a pipe.  
 $g$  = Acceleration due to gravity.  
 $h$  = Heat transfer coefficient.  
 $h_a$  = Heat transfer coefficient from air to surface.  
 $h_c$  = Heat transfer coefficient due to conduction.  
 $h_s$  = Heat transfer coefficient from steam to surface.  
 $h_r$  = Heat transfer coefficient due to radiation.  
 $H$  = Quantity of heat.  
 $i$  = Total radial flow of heat at distance  $r$  per unit length of pipe.  
 $ir_p$  = Total radial flow of heat at distance  $r_p$  per unit length of pipe.  
 $J$  = Mechanical equivalent of heat.  
 $K$  = Diffusion coefficient in turbulent flow.  
 $k$  = Thermal conductivity.  
 $l$  = Length of pocket or thermometer in live steam.  
 $L$  = Length of thermometer in pocket.  
 $m$  = Mass of fluid passing through unit area in unit time.  
 $M$  = Mass.  
 $p$  = Pressure.  
 $r$  = Radial distance from origin or pipe centre.  
 $r_0$  = Radius of lagging removed from pipe.  
 $r_p$  = Radius of pipe.

- $R$  = Reynolds number.  
 $N$  = Richardson number.  
 $S$  = Distance constant for heat diffusion in a pipe.  
 $s$  = Wall thickness of pipe.  
 $t$  = Time.  
 $t'$  = Time-constant.  
 $T$  = Temperature.  
 $T_l$  = Temperature of pocket tip.  
 $T_{p,l}$  = Pipe temperature, lagged.  
 $T_{p,u}$  = Pipe temperature, unlagged.  
 $T_\infty$  = Limit of  $T_l$  as  $l \rightarrow \infty$ .  
 $T_s$  = Steam temperature.  
 $T_m$  = Temperature of thermometer tip.  
 $T_p$  = Temperature of pipe wall.  
 $T_x$  = Maximum temperature-difference of steam across a pipe.  
 $\delta T = T_s - T_m$ .  
 $\Delta T = T_s - T_p$ .  
 $v$  = Velocity.  
 $\bar{v}$  = Mean velocity of flow in a pipe.  
 $w$  = Wall thickness of pocket or thermometer.  
 $x$  = Flow-direction co-ordinate.  
 $y$  = Transverse horizontal co-ordinate.  
 $z$  = Transverse vertical co-ordinate.  
 $X$  = Distance, in direction of flow, in pipe diameters.  
 $\beta$  = Molecular heat conductivity of steam.  
 $\rho$  = Density.  
 $\eta$  = Viscosity.  
 $\theta$  = Temperature of surroundings.  
 $\sigma$  = Stefan's constant.  
 $\tau$  = Shear force in a fluid.  
 $\xi$  = Distance along thermometer stem.

## (1) INTRODUCTION

A study of thermometers in pockets in industrial plant has been made by W. Fishwick<sup>1</sup> and by A. R. Aikman, J. McMillan and A. W. Morrison.<sup>2</sup> The conditions in the steam pipes of a power station are in a class alone, and the accurate measurement of temperatures in these circumstances is worthy of special study. It is of importance because the measurement and maintenance of efficiency in steam turbines depends on it and because recently, with the use of higher steam temperatures, the protection of pipe material from overheating has become vital.

Errors in measuring steam temperature can occur for the following reasons:

(a) The steam temperature may not be uniform and may be different at the measuring point from the true average. It has been suggested that two supplies of steam to a pipe do not mix readily and therefore that any initial temperature-difference may persist and cause errors in measurement. It has also been suggested that even in a long, single pipe the temperature distribution may not be uniform.

(b) The pocket containing the measuring instrument may not achieve the temperature of the steam.

(c) The measuring instrument may not achieve the temperature of the pocket.

Mr. Lucas and Mr. Peplow are at the Research Laboratories of the Central Electricity Authority.

(d) The measuring instrument may be inaccurate.

(e) The measuring instrument may have a slow response.

All these points are considered theoretically and results are given for a range of normal conditions. Practical work has been carried out at Deptford West Power Station to confirm the theoretical findings. Where figures and curves are given they are calculated for the steam conditions at Deptford West during the experiment (see Section 8), but as far as possible details are given for adjusting the figures for other conditions.

## (2) DIFFUSION OF HEAT IN A STEAM PIPE

If two supplies of steam at different temperatures join in a single pipe there are three possible results:

(a) The two streams may continue as virtually independent streams along the common pipe.

(b) The two streams may gradually merge into a single stream.

(c) The pipe may contain packets of hotter or cooler steam which are distributed at random in the pipe but tend to retain the temperature corresponding to their origins for a shorter or longer time.

In the first case, measured temperatures will vary according to whether the thermometer contacts one stream or the other.

In the second case, errors will be possible near the join of the pipes, but the magnitude of an error will decrease progressively downstream.

In the third case, variations of temperature will occur if the thermometer responds rapidly enough, but the mean of these variations or the reading of a sluggish thermometer will indicate the true mean temperature of the steam.

Since, in all practical cases, steam flow is turbulent, case (b) is the one which would be expected to occur and the vital information required is the minimum length of pipe which will ensure that the mixing has proceeded far enough for accurate temperature measurements to be made.

### (2.1) The Diffusion Coefficient

In any turbulent stream there is a continual interchange of fluid between adjacent parts of the stream. If one part of the stream is hotter than another it follows that hot fluid is moved to the cooler part, and vice versa, so that the temperatures tend towards equality. The speed of action of this process is expressed by a coefficient  $K$ , known as the diffusion coefficient, such that if  $C$  is the concentration of any property of a fluid (heat, momentum, etc.) the net amount of this property transferred per unit area per unit time in the  $z$  direction is  $-K(dC/dz)$ , where  $dC/dz$  is the change of concentration of the property with change of  $z$ . If  $C$  is the concentration of heat,  $C = \rho c_p T$ , and  $dH/dt$  per unit area is  $-K(dC/dz) = -K\rho c_p (dT/dz)$ . The rate of decrease with distance of heat-flow rate is

$$\frac{d^2 H}{dt dz} = K \rho c_p \frac{d^2 T}{dz^2}$$

This is equal to the rate of accumulation of heat.

$$\text{i.e. } \rho c_p \frac{dT}{dt} = K \rho c_p \frac{d^2 T}{dz^2}$$

from which

$$\frac{dT}{dt} = K \frac{d^2 T}{dz^2}$$

If we take the  $x$  direction along the axis of the pipe we have to consider diffusion in the  $y$  and  $z$  directions; the effect of diffusion in the  $x$  direction is negligible.

Hence the complete equation is

$$\frac{dT}{dt} = K \left( \frac{\partial^2 T}{\partial z^2} + \frac{\partial^2 T}{\partial y^2} \right)$$

However, if  $C$  is the concentration of momentum,  $C = \rho v$ . Then, by definition,

$$\frac{d(mv)}{dt} = -K \frac{d(\rho v)}{dz} = -\rho K \frac{dv}{dz}$$

and this is equal to the shear stress in the fluid. Now the shear stress and  $dv/dz$  in a pipe can be determined, and therefore  $K$  can be found in a practical case and applied to a study of the diffusion of heat. The general case is worked out in Section 12.1.

### (2.2) The Diffusion Equation

The basic differential equation for diffusion is

$$\frac{dT}{dt} = K \left( \frac{\partial^2 T}{\partial z^2} + \frac{\partial^2 T}{\partial y^2} \right)$$

$K$  can be considered as a known function of position and flow conditions in a pipe.

The general solution of the equation is not possible. The problem becomes simpler if the system is cylindrically symmetrical, i.e. if diffusion of heat occurs only radially. Thus it is not possible to deal with the case where the left-hand half of a pipe is hotter than the right-hand half, but it is possible to consider the case where the central half is hotter than the peripheral half. (Although this is not the form of problem presented by two steam pipes joining, there is little doubt that the results in the two cases will be of the same order and the solution for the artificial case will be a valuable guide for the practical case.) The solution is only possible analytically if  $K$  is assumed to be constant (see Section 12.2), but a graphical solution has been made without this restriction.

A description of the graphical method would be too long to be included in the paper, but the results are shown in Figs. 1 and 2 for conditions at Deptford West (see Section 8). They

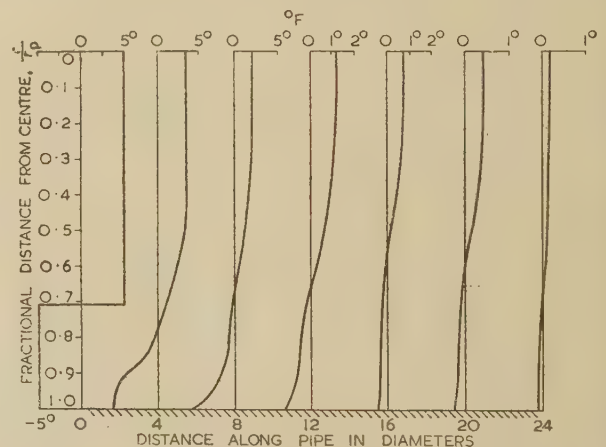


Fig. 1.—Change of temperature distribution along pipe.  
(Note change of temperature scale.)

$R = 2 \times 10^6$ .

For different Reynolds number apply correction in Table 1.

may be applied to other pipes and other fluid conditions provided that distances are given in pipe diameters and that the Reynolds number for the other conditions is approximately  $2 \times 10^6$ . The figures may be applied to conditions of smaller Reynolds number provided that the pipe distances are multiplied by the appropriate factors in Table 1.  $R = DM/\eta$  may be calculated by taking  $D$  in feet,  $M$  in pounds per square foot per hour and  $\eta$  in pounds per foot per hour from Table 2.

The generalized result may be stated thus: Any arbitrary

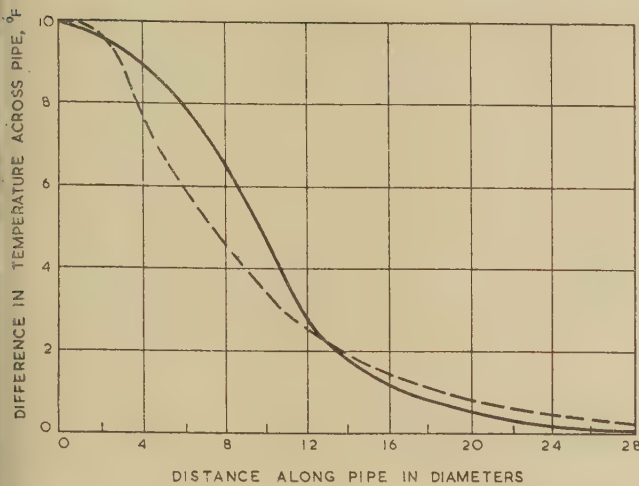


Fig. 2.—Change of temperature difference with distance along a steam pipe.

— Estimated graphically.  
 - - - Calculated as in Section 12.2.  
 See notes on Fig. 1.

Table 1

$R = \frac{D \epsilon \rho}{t_i} = \frac{DM}{t_i}$	Multiplier
$2 \times 10^6$	1
$2 \times 10^5$	0.8
$2 \times 10^4$	0.6
$2 \times 10^3$	0.2

Table 2

VISCOSITY OF SUPERHEATED STEAM

Temperature	Viscosity at		
	400 lb/in <sup>2</sup>	600 lb/in <sup>2</sup>	800 lb/in <sup>2</sup>
deg. F	lb/ft-h	lb/ft-h	lb/ft-h
500	0.066	0.075	—
600	0.068	0.076	0.085
700	0.070	0.078	0.086
800	0.074	0.081	0.089
900	0.078	0.085	0.094
1000	0.082	0.090	0.100

form of temperature distribution in a steam pipe changes to an equilibrium form in about 8 diameters. After this, although the form remains the same, the magnitude of the range of temperature decreases exponentially with a "distance constant" of 5-7 diameters, i.e.

$$T_{x1} = T_{x2} \exp \frac{x_1 - x_2}{S} \text{ and } S = 5-7D.$$

It is thus apparent that serious errors in temperature measurement when the steam comes from two sources can be avoided by making the measurement a suitable distance downstream from the join.

When the pipe has bends, or constrictions due to valves, etc., the length required is likely to be less than with a straight pipe.  $T_x$  will normally fall to 1% of its original value in 30 diameters or less.

It may be argued that if the hotter steam overlaid the cooler the inversion so formed might inhibit turbulence. The criterion used in the study of atmospheric inversions is that if the Richardson number is less than unity, turbulence will overcome the effect of the temperature gradient. For the steam pipe,

$$N = \frac{g \frac{\partial T}{\partial z}}{T \left( \frac{\partial v}{\partial z} \right)^2} \text{ is approximately } 10^{-4}$$

Hence it may be safely assumed that the temperature gradient will have a negligible tendency to inhibit turbulence.

Backward and forward diffusion of heat has been neglected in this discussion because  $dT/dx$  and  $d^2T/dx^2$  are insignificant compared with  $dT/dy$  and  $d^2T/dy^2$ , etc.

### (3) TEMPERATURE DISTRIBUTION OF A STEAM PIPE AND ITS LAGGING

It has been shown that any temperature gradient in the steam tends to be destroyed by the diffusion of heat. This was on the assumption that there was no radial escape of heat from the steam. Since the lagging of a steam pipe cannot be perfect there is invariably a flow of heat from the steam to the atmosphere. This implies temperature gradients in the steam, the steam pipe and its lagging. These gradients can be deduced from the flow of heat and the thermal resistances of the various parts of the system.

#### (3.1) Temperature Gradient in Steam

In Section 12.3, the distribution of flow across the pipe and the thermal resistance of the steam for a given heat flow out of the pipe are evaluated and hence the temperature gradient in the steam is determined. The results are shown plotted in Fig. 3.

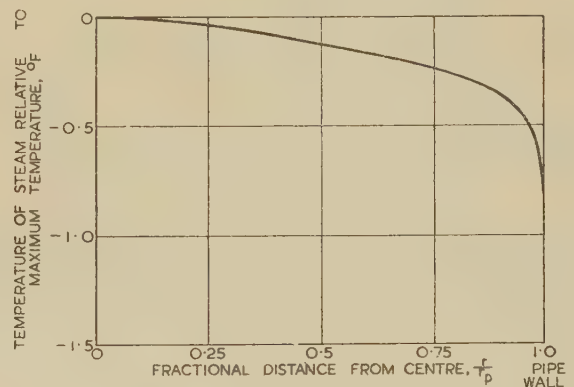


Fig. 3.—Temperature across a well-lagged steam pipe at 775°F and  $152 \times 10^3 \text{ lb/ft}^2\text{-h}$ .

For different radial heat-flow multiply figures on y-axis by  $\frac{\text{Heat flow}}{173 \text{ B.Th.U./h-ft}^2}$

For different mass flow multiply figures on y-axis by  $\frac{152 \times 10^3 \text{ lb/ft}^2\text{-h}}{\text{Mass flow}}$

They apply to conditions at Deptford West (see Section 8) for a fully lagged pipe. They can be applied to other pipes where the heat flow from the pipe per unit area is the same. For higher or lower heat flows the temperature differences will be proportionate to the heat flow. For completely unlagged pipes the temperature differences will be 20 or 30 times greater. For pipes with some lagging removed the differences will not approach the unlagged differences unless a considerable length of pipe is unlagged. The analysis makes no allowance for the effect of

radiation. This will in fact probably reduce the temperature differences by about 10% under average conditions.

### (3.2) Temperature Gradient near the Pipe Wall

It is apparent from Fig. 3 that the temperature gradient in the steam is small except near the walls. The method used to prepare Fig. 3 does not apply to points very close to the walls and it is better to use the method of film coefficients to estimate the total temperature difference between steam and pipe. The assumption is made that the thermal resistance between the steam and the pipe is confined to a layer of negligible thickness, and values for the thermal conductance of this layer are tabulated in reference books. For the conditions at Deptford West (see Section 8 and Reference 3) the internal film coefficient is 85 B.Th.U./ft<sup>2</sup>-h per deg. F. It is proportional to the 0.8 power of the mass flow and to the -0.2 power of the pipe diameter. The overall difference in temperature given in Fig. 3 has been obtained by this method.

### (3.3) Temperature Gradient outside the Pipe

The thermal resistance of the pipe metal itself can be neglected; that of the lagging is specified by the makers. In the case of Deptford West with three inches of magnesia plastic the resistance varies somewhat according to the working temperature of the material, but the conductance per unit area can be assessed at 0.2 B.Th.U./ft<sup>2</sup>-h per deg. F.

Finally, there is a film coefficient for the outside of the pipe which is difficult to specify because it varies with the draughtiness of the situation and other conditions. It will be taken as 1-3 B.Th.U./ft<sup>2</sup>-h per deg. F when the surface temperature is about 100° F, but may be 6.5 B.Th.U./ft<sup>2</sup>-h per deg. F when the surface temperature is 725° F, i.e. for the unlagged pipe.<sup>3</sup>

### (3.4) Difference between Steam Temperature and Pipe Temperature

The various thermal resistances per unit area are as follows: resistance of inner film =  $1/85 = 0.012^\circ \text{F-h/B.Th.U.}$ ; resistance of lagging =  $1/0.2 = 5^\circ \text{F-h/B.Th.U.}$ ; and resistance of outer film, say  $0.5^\circ \text{F-h/B.Th.U.}$ . The total resistance is  $5.512^\circ \text{F-h/B.Th.U.}$ . The total temperature drop is  $710^\circ \text{F}$ . Hence the drop from the steam to the pipe is  $0.012/5.512 \times 710 = 1.5^\circ \text{F}$ . This figure has been estimated for various steam flows and for the pipe lagged and unlagged. The results are shown in Figs. 4 and 5; the figures represent extremes. In practice, the temperature drop of the pipe will lie between the figures for the lagged and unlagged cases depending on the

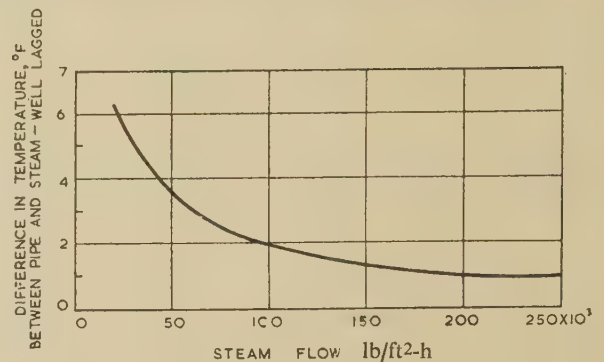


Fig. 5.—Effect of rate of flow of steam on the difference between pipe temperature and steam temperature—pipe lagged.

See notes under Fig. 4.

impairment of the lagging by pocket bosses, cracks in lagging, etc.

The results can be applied to pipes of different diameter, since the effect of diameter is small.

## (4) EFFECT OF THE PIPE TEMPERATURE ON STEAM TEMPERATURE MEASUREMENTS

### (4.1) Error due to Conduction to the Pipe

The fact that the pipe is cooler than the steam is of importance because any thermometer or thermometer pocket inserted into the steam is sure to be mechanically related to the pipe wall and will therefore be affected by the pipe temperature. In Fig. 6 is

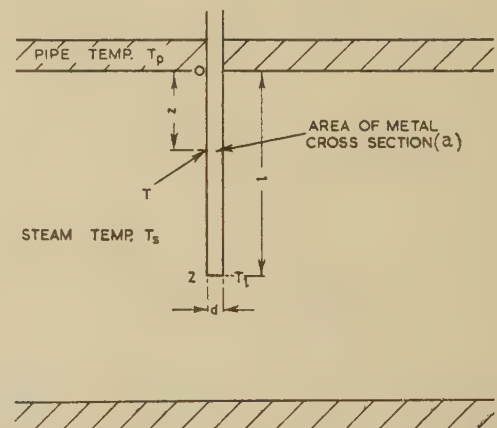


Fig. 6.—Schematic arrangement of pocket or thermometer and steam pipe.

$$\Delta T = T_s - T_p$$

$$\delta T = T_s - T_l$$

shown a projection from the pipe wall which may represent either a thermometer or a thermometer pocket. If the pipe is at the same temperature as the steam, the projection will also be at the same temperature (under equilibrium conditions). If the pipe is cooler than the steam by an amount  $\Delta T$ , heat is conducted away from the projection and the tip Z is cooler than the steam by an amount

$$\delta T = \frac{\Delta T}{\cosh \sqrt{\frac{\pi d h_s l}{a k}}} \quad (\text{see Section 12.4})$$

This is known as the conduction error.

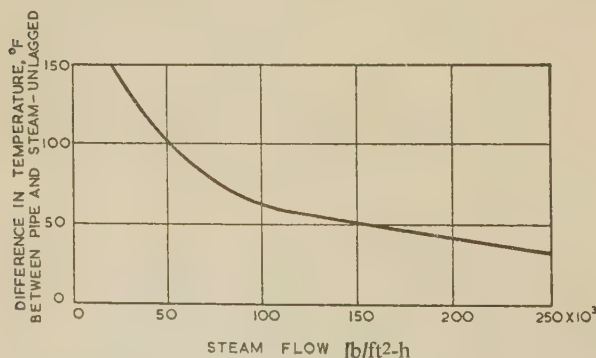


Fig. 4.—Effect of rate of flow of steam on the difference between pipe temperature and steam temperature—pipe unlagged.

For different steam temperature an approximate value can be obtained by multiplying figures on y-axis by  $\frac{T_s}{775^\circ \text{F}}$ .

Fig. 7 shows the values of  $\delta T$  obtained for various values of  $l$  with a mild-steel pocket of outside diameter  $\frac{7}{8}$  in and inside diameter  $\frac{1}{2}$  in for certain values of mass steam flow.

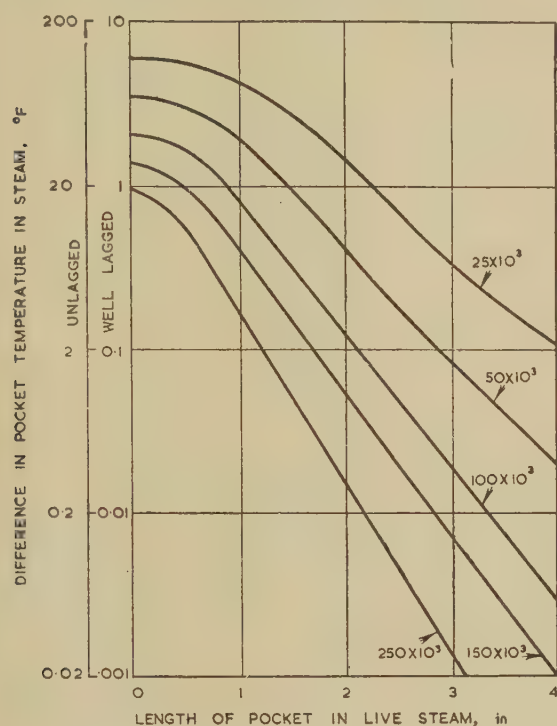


Fig. 7.—Change of pocket-conduction error with pocket length for various mass-flow rates (lb/ft<sup>2</sup>-h).

(For radiation error see Fig. 8.)

For pockets of different diameter multiply the figures marked on the x-axis by  $(\frac{7}{8}/D)^{0.3}$ .

For pockets of different cross-sectional area multiply figures marked on x-axis by  $(a/0.41 \text{ in}^2)^{1/2}$ .

For pockets of different thermal conductivity multiply figures marked on x-axis by  $(k/23 \text{ B.Th.U./h-ft per deg. F})^{1/2}$  (for stainless steel  $k = 12 \text{ B.Th.U./h-ft per deg. F}$ ).

A second ordinate scale is given which shows the approximate values of  $\delta T$  obtained with no lagging on the pipe. The two ordinate scales thus show the extremes possible. Practical cases will lie at points between the extremes depending on the extent to which the lagging falls short of the ideal case. In practice, unless all parts of the pocket are covered with three inches of lagging the temperature differences will be greater than the minimum. If there is an appreciable area of bare metal in the region of the pocket the temperature differences will approximate to the unlagged case (see Section 4.3).

For pockets of different diameter the figures marked on the x-axis should be multiplied by  $(\frac{7}{8}/d)^{0.3}$ ,  $d$  being in inches.

For pockets of different cross-sectional area the figures marked on the x-axis should be multiplied by  $(a/0.41)^{1/2}$ ,  $a$  being in square inches.

For pockets of different thermal conductivity the figures marked on the x-axis should be multiplied by  $(k/23)^{1/2}$ , the units of  $k$  being B.Th.U./ft-h-°F. It should be noted that for stainless steel  $k = 12$ .

#### (4.2) Error due to Radiation to the Pipe

The temperature of the tip of the pocket or thermometer is affected not only by conduction and convection but also by radiation between the three parts of the system—pipe, steam and thermometer or pocket. The pipe can be considered to

radiate as a black-body enclosure. The net effect of the steam radiation can be represented by considering it to have an emissivity of 0.6 (Reference 3).

The pocket or thermometer can be considered to have an emissivity of unity, since even stainless steel rapidly acquires a dark layer of tarnish at steam temperatures.

The net result can be represented by recognizing that the limiting temperature of the pocket as  $l \rightarrow \infty$  is no longer  $T_s$ —the steam temperature—but is less than this by  $[0.4 h_r / (h_s + h_r)] \Delta T$  (see Section 12.5). The effect of the steam flow rate on the radiation error is shown in Fig. 8.

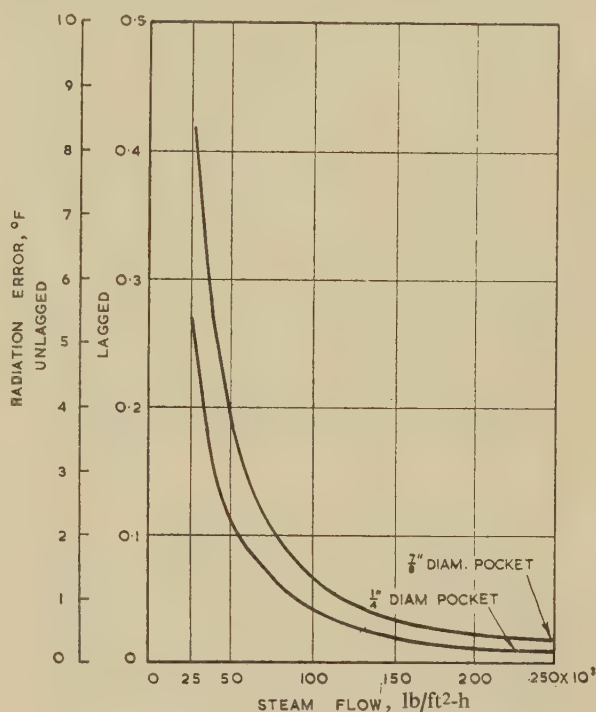


Fig. 8.—Dependence of radiation error on steam flow.

Steam temperature, 775° F.

For higher steam temperatures the error will be higher (see Section 12.5).

The very minute errors shown in Fig. 7 to be possible with long pockets are not realistic if radiation is allowed to play a part. They have merely been shown because it is possible in principle to reduce the effect of radiation with radiation shields or by rhodium-plating the pocket. The total effect of conduction error and radiation error is shown in Fig. 9.

#### (4.3) Effect of the Removal of Lagging

The removal of lagging over a small portion of the pipe reduces the pipe temperature, and may therefore increase temperature-measurement errors if the measurement is made in the unlagged portion. The effect is not, of course, the same as that of a completely unlagged pipe. It is of interest to know how large an area of pipe can be uncovered without appreciable effect and how large an area produces a fall of temperature near to that produced by the completely unlagged pipe. The matter is developed in Section 12.6, and the results are illustrated by Fig. 10, which shows the temperature distribution when (a) a disc of lagging of 1.6 in radius is removed and (b) a disc of lagging of 8 in radius is removed. Both figures apply to the pipe and steam conditions obtaining at Deptford West (see Section 8).

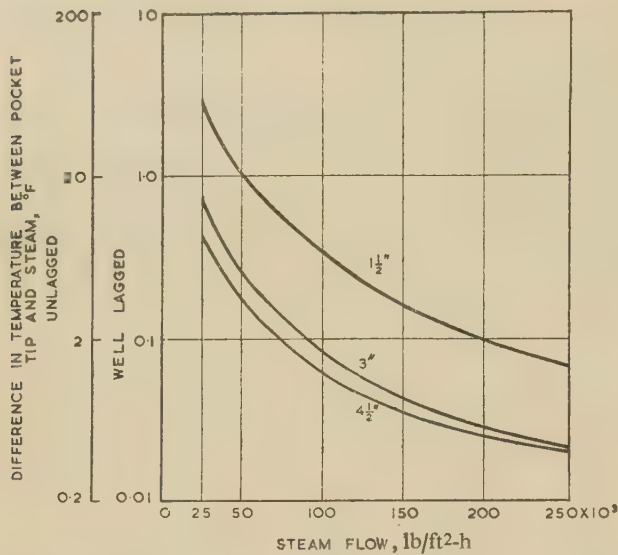


Fig. 9.—Combined conduction and radiation error for various pocket lengths.

For different conditions modify results of Fig. 7 and add to the errors of Fig. 8.

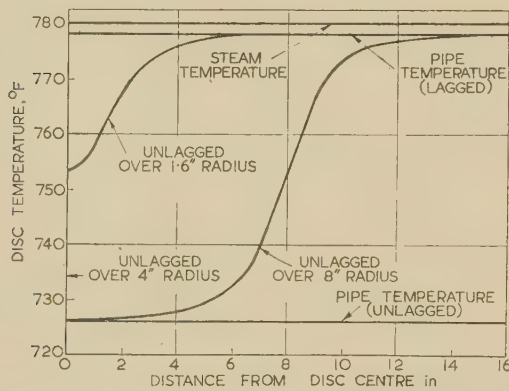


Fig. 10.—Temperature distribution from centre of an unlagged disc of pipe surface.

Steam flow rate,  $152 \times 10^3$  lb/ft²-h.

## (5) ERROR BETWEEN POCKET AND THERMOMETER

### (5.1) Error due to Conduction to the Air

If the thermometer itself is not inserted in the steam but is placed in a pocket (as is usual), there is a possibility that the thermometer may not assume the temperature of the tip of the pocket. For the sake of robustness, resistance thermometers or thermocouples are often sheathed in metal. It is usual for the sheath to extend from the tip of the pocket to a point, well outside the pocket, where it is cool enough for rubber-sheathed leads to be used. The arrangement is shown diagrammatically in Fig. 11.

The sheath protruding from the pocket is cooled by the atmosphere, and in consequence heat is conducted away from the thermometer tip. If this is in good metal-to-metal contact with the pocket no appreciable error will occur. This sort of contact is, however, most unlikely, since both pocket and thermometer will have coatings of oxide of relatively high resistance, unless plated with silver or rhodium. Thus, although metal-to-metal contact gives the best results, its attainment is so unlikely that it is better to rely on radiation and air-conduction

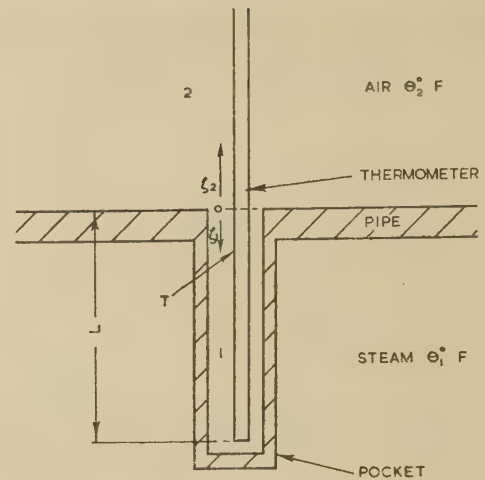


Fig. 11.—Schematic arrangement of pocket and thermometer.

heat transfer, which are very much more predictable. In most cases they will also be sufficient for accurate work.

It is necessary to consider the heat transfer all along the length of the thermometer sheath. At the entrance to the pocket the sheath is very much cooler than the pocket. There is therefore a very rapid transfer of heat to it, which reduces the heat flow along the sheath from the tip. At points further down the pocket the heat flow is still further reduced, and if the thermometer has a reasonable length compared with its conductivity it may approach pocket temperature very closely even without the benefits of metal-to-metal contact.

The problem is treated mathematically in Section 12.7, and it appears that it is easily possible to keep errors of this type very low. Fig. 12 shows the temperature distribution down the

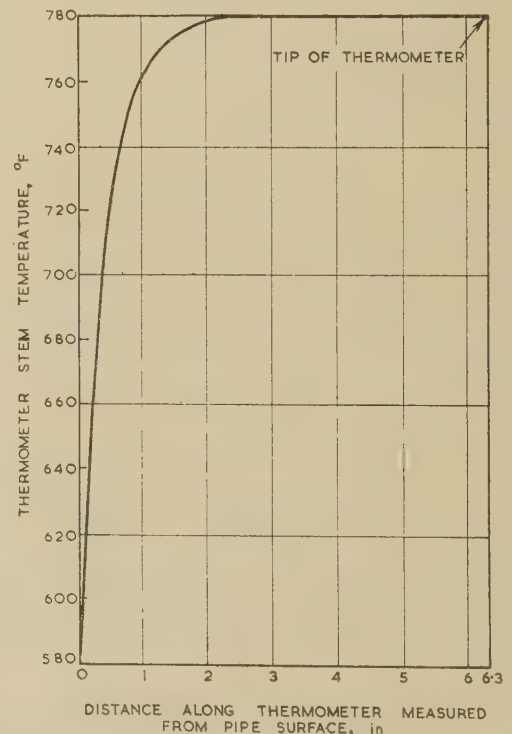


Fig. 12.—Temperature distribution along thermometer stem in a pocket.

For higher pocket temperature the error will be less.

For different conductivity of sheath multiply the figures marked on x-axis by  $(k/12B.Th.U./h-ft \text{ per deg. } F)^{1/2}$ .

For different sheath thickness multiply the figures marked on x-axis by  $(W/0.028)^{1/2}$ .

stem of a resistance thermometer in a pocket. Fig. 13 shows the error temperature for different lengths of resistance thermometer inserted in a pocket at 780° F.

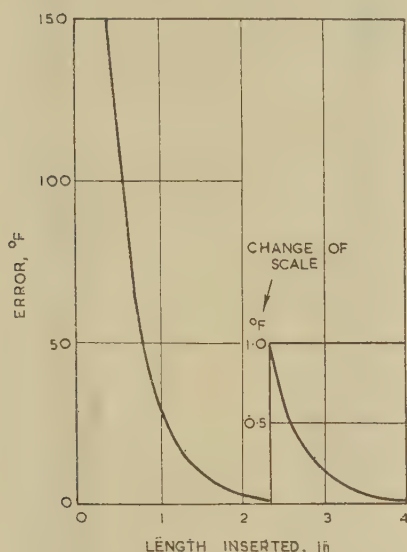


Fig. 13.—Error due to insufficient length of thermometer inserted in pocket at 780° F.  
See notes on Fig. 12.

It is often suggested that the error between the pocket and the thermometer may be reduced both by hindering the escape of hot air from (and the entry of cold air to) the pocket and by lagging the exposed portion of the thermometer. Thermometer lagging is considered in Section 12.7 and it is shown to have little effect. Reducing the error by air control has been studied experimentally and it has been found that by hindering the convection currents, with an asbestos plug, the minimum insertion for accurate results is about 3 in as calculated in Section 12.7, but that without the plug an insertion of 4 in is required. The internal diameter of the pocket was  $\frac{3}{4}$  in and the thermometer diameter in this experiment was  $\frac{1}{4}$  in.

#### (5.2) Error due to Time Lag

In all the factors so far considered it has been assumed that the conditions have been steady. If the steam temperature is rising or falling, another source of error becomes apparent. If the steam temperature suddenly changes to a new value it takes time for heat to flow from the steam to the thermometer to bring this to the new temperature. The larger the heat capacity of the thermometer, and the greater the thermal resistance through which the heat must flow, the longer the time which must elapse before the temperature is accurately indicated. If the steam temperature is rising steadily, the thermometer will read lower than the steam temperature by an amount which is proportional to the heat capacity of the thermometer, to the thermal resistance of the heat path and to the rate of change of temperature. (A similar effect is obtained when the more common case of a network of resistances and capacitances is concerned.) If the steam temperature varies sinusoidally about a certain mean temperature, the thermometer temperature will vary sinusoidally about the same mean temperature but with a smaller amplitude and with a certain lag in phase. Thus a thermometer which is accurate when the steam temperature is constant may have appreciable errors under normal conditions when the steam temperature rises and falls.

As stated above, a thermometer immersed in a pocket may give accurate readings even without metal-to-metal contact. Its time lag will, however, be somewhat greater than that of one with metal-to-metal contact and its errors on this account will be noticeably greater. The time lag of a thermometer immersed in the steam is very much smaller than that of one in a pocket and its time-lag errors will be negligible. This is probably the only respect in which an immersed thermometer is superior in accuracy to a properly designed pocket thermometer. It must be borne in mind that where steam temperatures are rising and falling about a predetermined temperature the lag errors tend to cancel out. Thus an average temperature obtained from many successive readings over a period of time will not suffer from the lag error which in a spot reading might be appreciable.

Where steam temperatures are measured to protect pipes and turbines from overheating the case is different. During the warming-up period, rates of change of temperature may be very great and errors due to the time lag of pocket thermometers may be as great as 20° F. However, here it must be borne in mind that the time lag of the pipe will certainly be greater than that of the pocket; the pipe will be at a still lower temperature than the thermometer and will not be endangered even by an apparently alarming error. Where a steam-temperature measurement is part of an automatic boiler-control system the time-constant of the thermometer may be a vital factor. The matter is complex and further consideration must be omitted.

The error caused by time lag is considered in more detail in Section 12.8, and practical results are given in Section 8.4.2.

### (6) OTHER FACTORS

#### (6.1) Temperature Equivalent of Steam Velocity

If the speed of steam along the pipe becomes comparable with the thermal velocities of its molecules it will affect the apparent temperature of the steam. This has been discussed by Murdock and Fiock,<sup>4</sup> who show that the difference in reading between a thermometer moving with the steam and a fixed one which takes full account of the steam velocity is given by  $v^2/2gJc_p$ . Under the conditions at Deptford West the expression has the value 0.17° F. A normal thermometer will read a temperature somewhere between the two extremes.

#### (6.2) Instrument Error

The accuracy of platinum resistance thermometers in indicating their own temperatures can be increased to a limit of about  $\pm 0.003^\circ$  F by increasing the accuracy of the measuring apparatus and improving the precautions taken, such as the maintenance of a constant ambient temperature. The accuracy of the arrangements used at Deptford West was probably  $\pm 0.2^\circ$  F.

The inherent accuracy of thermocouples is less than that of resistance thermometers. Under the very best practical conditions it is probably  $\pm 1^\circ$  F and will commonly be  $\pm 5^\circ$  F.

### (7) DESIGN OF TEMPERATURE MEASURING POINTS

#### (7.1) Conduction Error

The conduction error can be minimized by:

- Long pockets.
- Pockets of small metal cross-sectional area.
- Pockets of high thermal resistance, e.g. stainless steel.
- High steam velocity on pocket surface.
- Good lagging of pipe.

## (7.2) Radiation Error

Radiation error can be reduced by:

- (a) Good lagging of pipe.
- (b) High steam velocity.
- (c) Pockets of small diameter.
- (d) Pockets of low emissivity (rhodium plated).

## (7.3) Thermometer-to-Pocket Error

Thermometer-to-pocket error can be reduced by:

- (a) Long pockets.
- (b) Thermometers of small metal cross-sectional area.
- (c) Thermometers of high thermal resistance, e.g. stainless steel.
- (d) Short active elements.
- (e) Good thermal contact between thermometer and pocket.

## (7.4) Lag Error

Lag error can be reduced by:

- (a) Thermometers of low heat capacity.
- (b) Pockets of low heat capacity.
- (c) Total immersion of the thermometer in live steam.
- (d) Good thermal contact between thermometer and pocket.

The lag error is the only one which cannot easily be reduced to negligible proportions without total immersion of the thermometer in live steam. It is doubtful whether this benefit justifies the additional difficulties and risks of immersed thermometers except for experimental purposes.

## (8) PRACTICAL WORK

An experiment was carried out at Deptford West to measure temperature variation, if any, across a steam pipe after a junction and to compare the readings of thermometers immersed in the steam with those of a thermometer placed in a pocket. The conditions were as follows:

Steam pressure .. .. .	400 lb/in <sup>2</sup>
Steam temperature .. .. .	775° F.
Pipe diameter (internal) .. .. .	13 in.
Pipe area .. .. .	0.92 ft <sup>2</sup> .
Steam flow rate .. .. .	140 × 10 <sup>3</sup> lb/h.
Mass flow rate .. .. .	152 × 10 <sup>3</sup> lb/ft <sup>2</sup> -h.
Steam specific gravity .. .. .	0.0095.
Steam velocity .. .. .	65 ft/sec.
Pipe lagging .. .. .	3 in magnesia plastic.

## (8.1) Experimental Arrangement

It was hoped to use a pipe with a flanged joint to enable insertions to be made through a joint ring, but the only suitable pipes were of all-welded construction. To avoid the delay and cost of welding-in a special section, it was decided to insert the thermometers into a steam pipe by means of screwed plugs, fitted into pocket holes in the pipe, as shown in Fig. 14. The boiler chosen for the work was No. 18 at Deptford West, which had a number of suitable thermometer pockets.

Fig. 15 shows the two steam pipes from the boiler joining a common pipe at a point 20 ft from the boiler unit. The three pipes are shown in the same plane, but in fact they drop as they bend through the 90° angle.

The difference in steam temperature from the two feed pipes was measured with differential chromel-alumel couples inserted in two deep thermometer pockets. The steam temperature in the common pipe was measured with resistance thermometers. One was placed in a stainless-steel pocket, 5 in. long,  $\frac{9}{16}$  in inside diameter and  $\frac{1}{16}$  in outside diameter; eight others were introduced into the steam by means of two  $\frac{3}{4}$  in B.S.P. plugs,

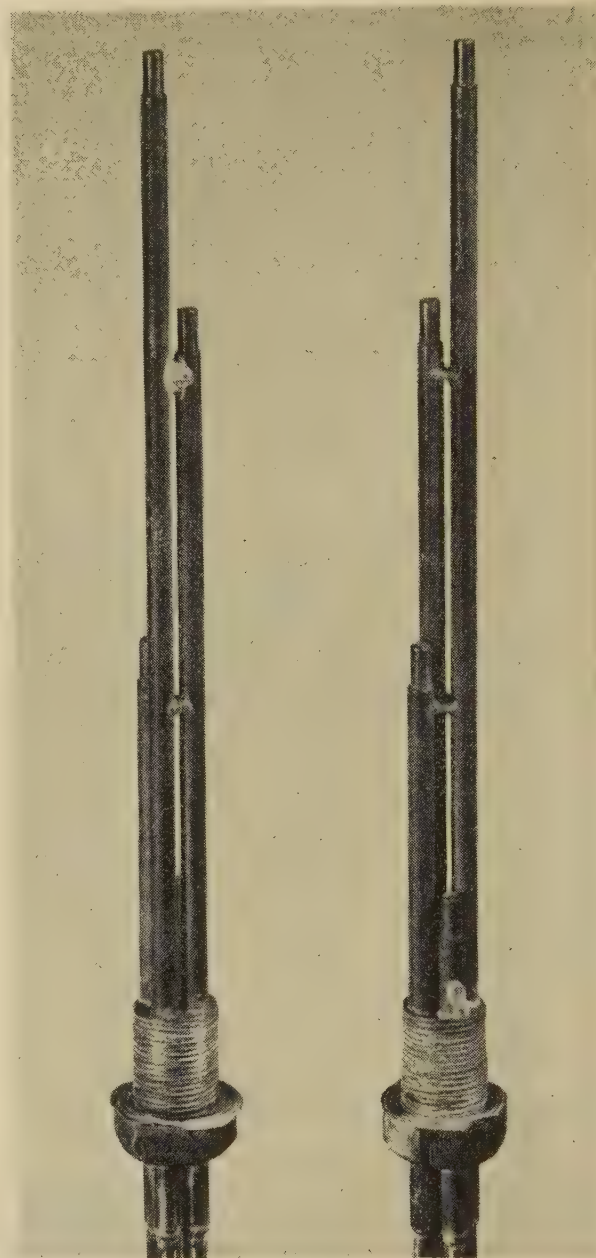


Fig. 14.—Method of mounting resistance thermometers in steam pipe.

one inserted horizontally and the other vertically, so that the measuring points were well distributed over the pipe cross-section as shown in Figs. 14 and 15.

The active element of the resistance thermometer is  $\frac{1}{4}$  in in length and is placed very near the tip of a stainless-steel tube  $\frac{1}{4}$  in in diameter and with a wall thickness of 0.028 in except at the element, where it is 0.014 in. The first time the experiment was attempted calibration of the thermometers was lost during their time in the steam pipe and in some cases they became open-circuited. This was attributed by the makers to vibration of the thermometers in the steam flow and to ingress of steam. In consequence, for the second attempt the thermometers were supported by mild-steel tubes and the tips were welded instead of being brazed. The experiment was carried out immediately after insertion. A week later the thermometers were removed from the steam pipe and recalibrated. Only the two longest thermometers had changed calibration appreciably and the

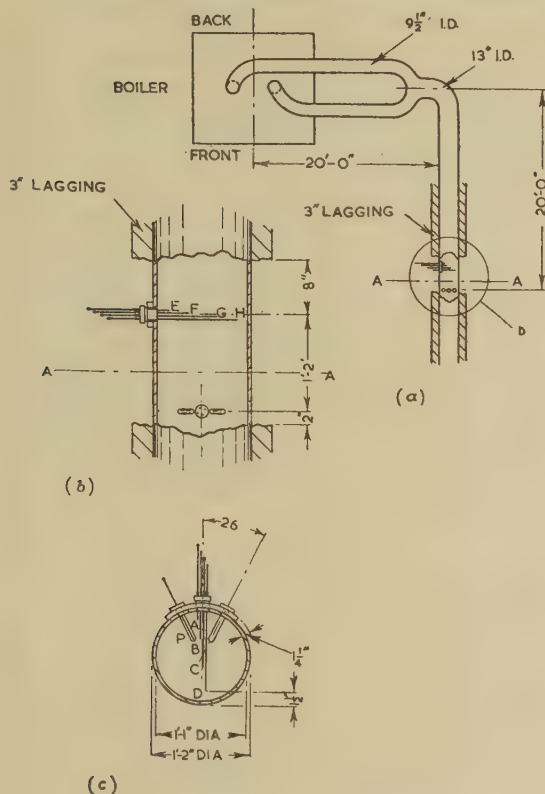


Fig. 15.—Experimental arrangement.

- (a) Pipework.  
 (b) Enlarged side section.  
 (c) Sectional plan A-A.
- Immersion distances:  
 A and E,  $1\frac{1}{2}$  in.  
 B and F,  $4\frac{1}{2}$  in.  
 C and G,  $8\frac{1}{2}$  in.  
 D and H,  $11\frac{1}{2}$  in.

change represented less than  $1^{\circ}\text{F}$  in each case. There were appreciable changes, however, in the insulation resistance of several thermometers, but it is fairly certain that this occurred in the period after the conclusion of the experiment.

The measuring circuit consists of a Wheatstone bridge with a resistance thermometer as the unknown arm. Each thermometer can be selected in turn by a switch. The bridge is balanced for a temperature somewhat above that of the steam, and the unbalance, due to the thermometers being at a lower temperature, is recorded on a high-speed millivolt recorder. Any two readings can be compared within a few seconds, and the set of nine can be scanned in half a minute.

Two methods of obtaining readings were used: (a) scanning each thermometer in turn and repeating the first reading at the end to enable an allowance for a general drift to be made (a variation on this was to reverse the scan and so obtain a double set of measurements); (b) scanning so that one particular thermometer was read at the start and again after each of the other thermometers was selected.

The difference in feed-pipe temperatures was read at the start and at the end of each run. At the end of a run the bridge was unbalanced by a few ohms to give a calibration signal on the recorder chart. Full sensitivity gave a deflection of 22 divisions ( $1\frac{1}{8}$  in) per degree F and so was adequate for the present work.

### (8.2) Precautions

The following precautions were taken:

(a) The strength of the thermometers was computed. A safety factor of 10 at  $400^{\circ}\text{C}$  was found.

(b) The silver-soldered plug assembly was pressure-tested.

(c) The remote end of each thermometer was kept below  $200^{\circ}\text{C}$  to prevent the melting of soft solder and deterioration of the leads. A minimum projection of 3 in above the steam-pipe lagging is required.

(d) Thermal electromotive forces were avoided in the bridge circuit.

(e) A selector switch of negligible contact resistance was used.

(f) The bridge current was made a few milliamperes only, in order to keep self-heating error in the thermometer below  $0.2^{\circ}\text{F}$ .

(g) The thermometers were compared at  $60^{\circ}\text{F}$ , at  $212^{\circ}\text{F}$  and at the working temperature before and after use. This calibration could be repeated to within  $\pm 0.2^{\circ}\text{F}$ , and the spread between thermometers was  $\pm 0.7^{\circ}\text{F}$ . The thermometers were recalibrated at room and steam temperatures after silver-soldering into the plugs.

Measurements were commenced before the boiler came on load and were continued at intervals as the boiler gradually took up normal working and then operated for some time under steady conditions.

### (8.3) Results

All temperature readings are compared with the reading of thermometer F (Fig. 15), which was ideally placed for accurate measurement and had no measurement change of calibration after the experiment. The results are given in Table 3. The figures in the third column give the difference in temperature between the two steam supplies.

### (8.4) Discussion of Results

#### (8.4.1) Conduction, Radiation and Mixing Errors.

It is apparent that when the steam flow was low there were considerable differences between the thermometers. As the steam flow-rate increased the readings agreed more and more closely, until under normal conditions six of the thermometers agreed within  $\pm 0.35^{\circ}\text{F}$ . This supports the proposition that, except near the walls of the pipe, the steam temperature is very nearly uniform. The thermometers A and E are in close agreement with each other but are lower than the others. These were the shortest thermometers, and although their length would have been adequate while unsupported, the addition of the mild-steel supporting tubes produced an appreciable error. Using the theory given in this report the error to be expected for these thermometers is  $0.5^{\circ}\text{F}$ . This is close to the differences measured. The pocket thermometer also reads lower than the completely immersed thermometers. The estimated conduction error for the pocket is negligible; the estimated error between pocket and thermometer is also negligible, but the radiation error is estimated to be  $0.5^{\circ}\text{F}$  greater for the pocket than for the more slender thermometers. This again agrees well with the measured difference. It must be pointed out that the lagging was removed from the pipe in the measurement region. The errors measured, therefore, although small, are larger than would be experienced under normally good lagging conditions.

Although there were considerable differences in temperature between the two supplies of steam, there is no evidence that this difference has persisted in any degree as far as the measuring point. There is also no evidence that a change in the temperature difference produces a change in the temperature pattern at the measuring point. This supports the view that mixing in a straight pipe as shown in Fig. 2 is slower than in the Deptford West case, where mixing was probably assisted by bends in the pipe.

Table 3

Time	Run	Temp. diff.	F	Temperature relative to F									Remarks
				A	B	C	D	E	F	G	H	P	
hours GMT		°F	°F	°F	°F	°F	°F	°F	°F	°F	°F	°F	
0625	1	12	450	-21	-0.3	-2.0	-16	-32	0	0	-0.3	-3.2	Steam stagnant
0630	2	—	—	—	—	—	—	—	—	—	—	—	Start of steam flow
0635	3	30	662	-16.6	-11.4	-3.2	-1.4	-6.5	0	+0.5	1.3	-28	Steam flow increasing
0638	4	34	705	-4.7	-0.4	+0.2	+0.1	-5.4	0	-0.4	-0.3	-13.1	Steam flow increasing
0640	5	36	712	-4.0	0	0	+0.2	-4.5	0	-0.3	-0.5	-7.7	Steam flow increasing
0645	6a	36	722	-2.9	+0.1	+0.2	+0.3	-6.5	0	-0.4	-0.3	-4.5	Steam flow increasing
—	6b	36	722	-2.3	+0.3	0	+0.4	-4.7	0	-0.2	0	-3.8	Steam flow increasing
0655	7	—	—	—	—	—	—	—	—	—	—	—	Steam flow increasing
0715	8	17	802	-1.8	+0.1	+0.3	+0.7	-2.2	0	-0.2	+0.5	-3.1	Steam flow increasing
—	9	—	—	—	—	—	—	—	—	—	—	—	Steam flow increasing
0721	10a	12	802	-1.1	+0.4	+0.5	+0.7	-1.3	0	0	+0.5	-0.5	Steam flow increasing
—	10b	11	802	-1.1	+0.2	+0.3	+0.5	-1.3	0	0	+0.5	0	Steam flow increasing
—	11	—	—	—	—	—	—	—	—	—	—	—	Steam flow increasing
0725	12a	11	802	-0.7	+0.4	+0.2	+0.5	-0.3	0	+0.4	+0.5	-0.5	Flow steady at $140 \times 10^3$ lb/h
—	12b	11	802	-0.7	+0.3	+0.4	+0.7	-0.3	0	+0.4	+0.5	-0.7	
—	13	—	—	—	—	—	—	—	—	—	—	—	
0800	14	12	802	-0.9	0	+0.1	+0.5	-0.9	0	+0.5	+0.3	-0.7	
0850	16	28	795	-0.7	0	0	+0.4	-0.5	0	+0.5	+0.3	0	Pocket thermometer not in metallic contact
0915	18	-6	786	-0.4	0	+0.2	+0.7	-0.2	0	—	+0.9	+0.9	
1110	20	15	786	-0.7	-0.2	+0.1	+0.7	-0.5	0	+0.7	+0.5	-0.3	Pocket thermometer not in metallic contact
1325	21	35	788	-0.2	+0.4	+0.5	+1.1	-0.2	0	+0.7	+0.7	+0.2	Pocket thermometer not in metallic contact
1330	22	36	781	-0.7	0	+0.1	+0.9	-0.7	0	+0.9	+0.5	0	Pocket thermometer not in metallic contact
1430	23	42	783	-0.9	+0.2	+0.1	+0.9	-1.1	0	+0.5	+0.5	-0.7	Pocket thermometer not in metallic contact
—	—	—	—	-0.7	0.1	0.2	0.7	-0.6	0	0.5	0.6	-0.2	Average of last nine runs

## (8.4.2) Errors due to Time Lag.

It was found that when the steam temperature varied sinusoidally with an amplitude of 9°F and a period of 18 min the measured temperature had an amplitude of 8°F and a lag of 1 min. The maximum disagreement was nearly 3°F.

Using the expression derived in (a) of Section 12.8 the time lag of the immersed thermometer has been estimated to be a few seconds. Its errors due to lag are therefore negligible. Using the expressions derived in (b) and (c) of the same Section, the time-constant for the pocket thermometer was in both cases found to be 0.7 min. A similar measurement was made with the thermometer placed in the pocket without metallic contact; the time-constant was 1.0 min. This demonstrates that metallic contact in practice is useful but does not greatly outweigh the heat transfer due to radiation and convection. It is of interest to note that under steady conditions it made no difference to the temperature reading whether there was metallic contact or not.

If the temperature is rising steadily the lag error is  $t' \times dT_s/dt$ . The maximum rate of rise of temperature experienced at Deptford West after the boiler had settled down was 2.2°F/min. This implies an error of 1.5°F.

If the temperature is fluctuating sinusoidally the error may be estimated from the value of  $t' \times \max dT_s/dt$ , if  $t'$  is small compared with the time of the temperature cycle. This will tend to exaggerate the error if anything. It may be estimated more accurately as in (c) of Section 12.8.

## (9) CONCLUSIONS

Two streams of steam entering a single pipe are thoroughly mixed in a distance which can be estimated and which is usually about 30 diameters.

The temperature gradients in high-velocity steam in a well lagged pipe are negligible except near the pipe wall.

Errors of steam-temperature measurement using pockets or immersed thermometers are calculated and shown in Figs. 1-13. They can be made negligible by proper design, with the exception of errors due to time lag, which are usually not large and can be allowed for.

Thermometers immersed in the steam can be very accurate, but the additional difficulty and risk are not justified by the additional accuracy under normal working conditions.

## (10) ACKNOWLEDGMENTS

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## (12) APPENDICES

## (12.1) Diffusion Coefficient in a Pipe

The shear force  $\tau$  in a fluid is equal to the rate of transfer of momentum, i.e.  $\tau = \rho K(dv/dr)$ . Considering a cylinder of fluid of radius  $r$  and unit length, the force tending to accelerate it is  $(dp/dx)\pi r^2$ . The retarding force is  $2\pi r\tau$ . Hence  $2\pi r\tau = \pi r^2 dp/dx$ . Therefore

$$\tau = \frac{r}{2} \frac{dp}{dx}$$

But  $\tau = \rho K dv/dr$  and hence

$$K = \frac{r}{2\rho} \frac{dp}{dx} \bigg/ \frac{dv}{dr}$$

The velocity distribution under turbulent flow conditions in a pipe is known to be of the form  $v \propto (r_p - r)^{1/7}$ . The average rate of flow is

$$\frac{1}{\pi r_p^2} \int 2\pi r v dr = \bar{v}$$

Hence

$$v = 1.22\bar{v} \left(1 - \frac{r}{r_p}\right)^{1/7}$$

$$\frac{dv}{dr} = -0.174 \frac{\bar{v}}{r_p} \left(1 - \frac{r}{r_p}\right)^{-6/7}$$

and

$$K = 2.98 \frac{dp}{dx} \frac{r_p}{\bar{v}} \frac{r_p}{\rho} \left(1 - \frac{r}{r_p}\right)^{6/7}$$

Now

$$\frac{dp}{dx} = \frac{f\bar{v}^2\rho}{r_p}$$

where  $f$  is a function of  $R$  which changes slightly with large changes of  $R$  (Table 1 shows the changes due to changes in  $f$ ). Hence  $K$  is known for all positions of the pipe and for all values of flow variables, and may be expressed by

$$K = 2.98 f \bar{v} r \left(1 - \frac{r}{r_p}\right)^{6/7}$$

By dimensional analysis it may be shown from this that if lengths along the pipe are expressed in diameters the result given in Figs. 1 and 2 is a general result for all pipes and all fluids, except for variations in  $f$ . These variations are relatively small and are dependent on Reynolds number only (see Table 1).

## (12.2) Diffusion in a Cylindrical Steam Pipe

The differential equation of Section 2.2 can be put in the form

$$\frac{\partial T}{\partial x} \simeq \frac{K}{\bar{v}} \left( \frac{\partial^2 T}{\partial r^2} + \frac{1}{r} \frac{\partial T}{\partial r} \right)$$

A solution of the form  $T = F(x, r)$  is required, the initial conditions being that the central half of the pipe is  $10^\circ\text{F}$  hotter than the rest.

The solution<sup>5</sup> is

$$T = \frac{2}{r_p^2} \left[ \int_0^{r_p} r f(r) dr + \sum_{n=1}^{\infty} \exp\left(-\frac{K\alpha_n^2 x}{\bar{v}}\right) \frac{J_0(\alpha_n r)}{J_0^2(\alpha_n r_p)} \int_0^{r_p} r f(r) J_0(\alpha_n r) dr \right]$$

where  $f(r)$  defines the initial temperatures,  $J_0$  and  $J_1$  are Bessel functions of the first kind of order 0 and 1, and  $\alpha_n r_p$ ,  $n = 1, 2, \dots$ , are the positive roots of  $J_1 = 0$ .

Therefore,

$$T = \frac{10\sqrt{2}}{r_p} \sum_{n=1}^{\infty} \left[ \frac{1}{\alpha_n} \frac{J_0(\alpha_n r) J_1(\alpha_n 0.71 r_p)}{J_0^2(\alpha_n r_p)} \exp\left(-X \frac{2r_p K \alpha_n^2}{\bar{v}}\right) \right]$$

where  $X$  is now the distance down the pipe in pipe diameters.

For the conditions at Deptford West  $K = 40r(1 - r/r_p)^{6/7}$  and the diffusion is thus zero at the centre and very close to the walls. Elsewhere  $K$  is high and has an average value of 120. At the initial temperature-boundary it is 160. With the former value, the results shown as the dotted curve of Fig. 2 are obtained.

For  $X > 7$ , the effects of the small roots ( $n > 2$ ) are negligible and the maximum difference in temperature over the pipe becomes

$$T_x = 15 \exp\left(-\frac{X}{7}\right)$$

## (12.3) Temperature Gradient in a Steam Pipe

The quantity of steam per second entering an annulus defined by  $dr$  is  $2\pi r v p dr$ . The heat lost by it per second is  $2\pi r v \rho c_p (dT/dx) dr dx$ , where  $dT/dx$  is the temperature gradient along the pipe. If  $i$  is the total radial flow of heat at distance  $r$  per unit length of pipe

$$\frac{di}{dr} = 2\pi r v \rho c_p \frac{dT}{dx}$$

$$= 2\pi r 1.22\bar{v} \left(1 - \frac{r}{r_p}\right)^{1/7} \rho c_p \frac{dT}{dx} \quad (\text{see Section 12.1}).$$

Hence

$$i = 2.44\pi \bar{v} \rho c_p \frac{dT}{dx} \int r \left(1 - \frac{r}{r_p}\right)^{1/7} dr, \quad \text{and } i = 0 \text{ when } r = 0.$$

We thus have

$$i = 2.44\pi \bar{v} \rho c_p \frac{dT}{dx} \left[ \frac{49}{120} r_p^2 - \frac{49}{120} r_p^2 \left(1 - \frac{r}{r_p}\right)^{15/7} - \frac{7}{8} r r_p \left(1 - \frac{r}{r_p}\right)^{8/7} \right];$$

$$i_{r_p} = 2.44\pi \bar{v} \rho c_p \frac{dT}{dx} \left( \frac{49}{120} r_p^2 \right)$$

and

$$i = i_{r_p} \left[ 1 - \left(1 - \frac{r}{r_p}\right)^{15/7} - \frac{120}{56} \frac{r}{r_p} \left(1 - \frac{r}{r_p}\right)^{8/7} \right]$$

The thermal conductivity of steam is  $K\rho c_p + \beta$ , where  $\beta$  is molecular conductivity.

The conductivity of a shell of unit length and unit thickness is  $2\pi r(K\rho c_p + \beta)$ , or

$$2\pi r (2.98 f \bar{v} r \left(1 - \frac{r}{r_p}\right)^{6/7} \rho c_p + \beta)$$

Hence the temperature gradient in the steam is

$$\frac{dT}{dr} = \frac{i_{r_p} \left[ 1 - \left(1 - \frac{r}{r_p}\right)^{15/7} - \frac{120}{56} \frac{r}{r_p} \left(1 - \frac{r}{r_p}\right)^{8/7} \right]}{2\pi r \left[ 2.98 f \bar{v} r \left(1 - \frac{r}{r_p}\right)^{6/7} \rho c_p + \beta \right]}$$

For the conditions at Deptford West the values were:

$$i_{r_p} = 525 \text{ B.Th.U./h-ft for well-lagged pipe.}$$

$$i_{r_p} = 10\,500 \text{ B.Th.U./h-ft for unlagged pipe.}$$

## (12.4) Conduction Error of a Pocket or Thermometer immersed in a Steam Pipe

The case considered is shown in Fig. 6. The heat flow along the stem is

$$F = -a \frac{dT}{dz} k$$

The heat flow from the steam into an increment of length  $dz$  is  $dF = -\pi dh_s(T_s - T)dz$ . But

$$\frac{dF}{dz} = -a \frac{d^2T}{dz^2} k$$

Hence 
$$\frac{d^2T}{dz^2} + \frac{\pi dh_s T}{ak} - \frac{\pi dh_s T_s}{ak} = 0$$

Solving this equation and considering that when  $z = 0$ ,  $T = T_p$  and  $dF/dz = 0$  when  $z = l$ , we have

$$T = T_s + (T_p - T_s) \left[ \cosh \sqrt{\left(\frac{\pi dh_s}{ak}\right) z} - \tanh \sqrt{\left(\frac{\pi dh_s}{ak}\right) l} \cosh \sqrt{\left(\frac{\pi dh_s}{ak}\right) z} \right]$$

When  $z = l$ , we have

$$T_s - T_l = \frac{T_s - T_p}{\cosh \sqrt{\left(\frac{\pi dh_s}{ak}\right) l}}$$

so that

$$\delta T = \frac{\Delta T}{\cosh \sqrt{\left(\frac{\pi dh_s}{ak}\right) l}}$$

### (12.5) Error due to Radiation

The radiation per unit area per unit time from a black body at  $T_1$  to an enclosure at  $T_2$  is given by

$$\sigma(T_1^4 - T_2^4)$$

When  $T_1 - T_2$  is small the radiation is

$$4\sigma T_1^3(T_1 - T_2)$$

or

$$h_r = 4\sigma T_1^3$$

For most practical cases the emissivity of steam in a pipe<sup>3</sup> can be taken as 0.6. The limiting temperature of the pocket or thermometer,  $T_\infty$ , as  $l \rightarrow \infty$ , is no longer  $T_s$  as in Section 12.4 but is such that

$$(T_s - T_\infty)(h_s + 0.6h_r) = (T_\infty - T_p)(0.4h_r)$$

Hence

$$T_\infty = T_s - \frac{(T_s - T_p)0.4h_r}{h_s + h_r}$$

and the temperatures obtained in practice will always be lower than those calculated by the formula of Section 12.4 by

$$\frac{0.4h_r}{h_s + h_r} \Delta T$$

It is true that radiation also increases the heat transfer from steam to pipe and from steam to pocket. The addition will normally be less than 10% and has been neglected.

### (12.6) Wall Temperature of a Partly Unlagged Steam Pipe

The system considered is a steam pipe with a disc-shaped patch of lagging removed. The heat flow per second into a thin annulus concentric with the centre of the exposed area is

$$2\pi r dr h_s(T_s - T_p) + 2\pi r s k dr \left( \frac{d^2T_p}{dr^2} + \frac{1}{r} \frac{dT_p}{dr} \right)$$

The heat flow out is  $2\pi r dr h_a(T)$  and in equilibrium this equals the heat flow in. Then a solution is required of the expression

$$\frac{d^2T_p}{dr^2} + \frac{1}{r} \frac{dT_p}{dr} - \left( \frac{h_s + h_a}{sk} \right) T_p + \frac{h_s T_s}{sk} = 0 \quad 0 \leq r \leq r_0$$

$$\frac{d^2T_p}{dr^2} + \frac{1}{r} \frac{dT_p}{dr} - \left( \frac{h_s + h'_a}{sk} \right) T_p + \frac{h_s T_s}{sk} = 0 \quad r \geq r_0$$

The boundary conditions are

- $dT_p/dr = 0$  at  $r = 0$ .
- $dT_p/dr = 0$  at  $r = \infty$ .
- $T_p$  is single-valued at  $r = r_0$ .
- $dT_p/dr$  is single-valued at  $r = r_0$ .

The following solution is obtained:

$$0 \leq r \leq r_0: T_p = T_{p,u} + (T_{p,l} - T_{p,u}) \alpha I_0(br)$$

$$r \geq r_0: T_p = T_{p,l} - (T_{p,l} - T_{p,u}) \alpha K_0(Br) \frac{I_1(br_0)}{K_1(Br_0)}$$

where

$$\alpha = \frac{K_1(Br_0)}{K_1(Br_0)I_0(br_0) + K_0(Br_0)I_1(br_0)}$$

$$b = \sqrt{\frac{h_s + h_a}{sk}} \quad B = \sqrt{\frac{h_s + h'_a}{sk}}$$

$I_0, I_1, K_0, K_1$  are modified Bessel functions of zero and the first order. Fig. 10 gives the results in graphical form for the conditions at Deptford West in the two cases  $r_0 = 1.6$  in and  $r_0 = 8$  in.

In using the above formula to obtain the temperature of the centre of the unlagged patch it is useful to remember that  $I_0(0) = 1$ .

### (12.7) Conduction Error of a Thermometer in a Thermometer Pocket

Consider a thermometer inserted in a pocket, which will be called region 1, and extending into the atmosphere, called region 2. Then the heat balance is

$$\pi d w k \frac{d^2T}{d\xi^2} = \pi dh(T - \theta)$$

with solution of the form  $T = \theta + A e^{n\xi} + B e^{-n\xi}$ , where  $T$  is the temperature at a point on the thermometer stem,  $n$  is equal to  $\sqrt{(h/kw)}$  and  $A$  and  $B$  are constants.  $\xi_1$  and  $\xi_2$  are taken as zero at the boundary of regions 1 and 2.

Now  $h_1 = h_r + h_c = (12.5 + 4.4) \text{ B.Th.U./ft}^2\text{-h}^\circ\text{F}$  where  $r$  and  $c$  denote radiation and conduction (air) and  $h_r$  is the mean of the top and bottom of pocket values (see Sections 11.8 and 13.2). The value of  $h_2$  is approximately 2.9.

The boundary conditions are:

- $T_2 = \theta_2$  at  $\xi_2 = \infty$ ; hence  $A_2 = 0$ .
- $\left| \frac{dT_1}{d\xi_2} \right| = \left| \frac{dT_2}{d\xi_2} \right|$  at  $\xi = 0$ .
- $T_1 = T_2$  at  $\xi = 0$ .
- $\frac{dT_1}{d\xi} = 0$  at  $\xi_1 = L$ , the length of thermometer inserted.

Therefore,

$$T_1 = \theta_1 - \frac{n_2(\theta_1 - \theta_2)[\varepsilon^{n_1\xi_1} + \varepsilon^{n_1(2L-\xi_1)}]}{n_2 - n_1 + (n_1 + n_2)\varepsilon^{2n_1L}}$$

$$\simeq \theta_1 - \frac{n_2(\theta_1 - \theta_2)}{n_1 + n_2} [\varepsilon^{n_1(\xi_1-2L)} + \varepsilon^{-n_1\xi_1}]$$

$$T_2 = \theta_2 + \frac{n_1(\theta_1 - \theta_2)(\varepsilon^{2n_1L - n_2\xi} - \varepsilon^{-n_2\xi})}{n_2 - n_1 + (n_1 + n_2)\varepsilon^{2n_1L}}$$

$$\simeq \theta_2 + \frac{n_1(\theta_1 - \theta_2)}{n_1 + n_2} [\varepsilon^{-n_2\xi} - \varepsilon^{-(2n_1L + n_2\xi)}]$$

For the work described, with  $L = 6.3$  in, the value of  $\theta_1 - T_{\xi=L}$  is  $3.6 \times 10^{-5}^\circ\text{F}$  and  $T_{\xi=0}$  is approximately  $\frac{5\theta_1 + 2\theta_2}{7} = 574^\circ\text{F}$ . A graph is given in Fig. 12.

For given values of  $\theta_1$  and  $\theta_2$ , the length which  $L$  must exceed to keep  $\theta_1 - T_{\xi=L} < 0.2^\circ\text{F}$  can be computed. For Deptford West it is 3.0 in.

If the thermometer stem in region 2 is well lagged,  $n_2$  is halved, and it can be shown that  $L$  must then be  $> 2.8$  in for  $\theta_1 - T_{\xi=L} < 0.2^\circ\text{F}$ . The advantage gained is negligible. The error due to insufficient insertion length is

$$\theta_1 - T_{\xi=L} = \frac{2n_2(\theta_1 - \theta_2)\varepsilon^{-n_1L}}{n_1 + n_2}$$

This is shown in Fig. 13.

#### (12.8) Thermometer Time Lag

A thermometer immersed in steam obeys the equation  $cdT_m = (T_s - T_m)h_s dt$ .

### DISCUSSION BEFORE THE SUPPLY SECTION, 14TH DECEMBER, 1955

**Mr. Llewellyn Young:** The accurate measurement of steam temperature has attained the greatest importance, first because temperatures are now approaching the practical maximum values for commercial metals and secondly because the large metal masses of modern turbines necessitate a consistent steam temperature.

In setting down those features which affect the accurate determination of steam temperature, and in providing the major points of theoretical treatment, the authors have not overlooked the practical aspect. It is probable that a rigorous mathematical solution is not only impossible but, from the practical point of view, unessential. It is first and foremost necessary that those practical enemies of robustness, namely sensitivity and accuracy, have to be reconciled, in which case it is necessary to provide a pocket and measuring element not only of low heat capacity and low thermal resistance but also of robust construction.

The authors have given as their view that the time lag due to metal to metal contact is an unreliable improvement over the definite air-gap. It would, however, seem reasonable to assume that any reduction in air-gap would be advantageous in reducing measurement lag, and experience has shown that spring-type thermal conducting strips, while providing valuable support to the element, also reduce the time lag of measurement. In particular the flat-type element described by Hornfeck\* shows markedly improved characteristics through the use of the conducting strip.

Throughout the experiment the authors appear to have employed resistance-thermometer elements, stating as reasons for their preference the higher intrinsic accuracy of this device over the thermocouple. While this is undoubtedly true for the steam conditions stated in the paper, resistance elements would, of course, be unsuitable for use at present-day temperatures and are generally too fragile for the duty when vibration is experienced. Vibration is, unfortunately, one of the greatest problems of temperature measurement to-day.

Hence it can be shown that:

(a) If  $T_s$  is constant

$$T_m = T_s(1 - \varepsilon^{-t/t'}) \text{ where } t' = c/h_s$$

and

$$t' = \frac{t_2 - t_1}{\log_e \frac{T_s - T_{m1}}{T_s - T_{m2}}}$$

(b) If  $\frac{dT_s}{dt}$  is constant

$$T_m = T_s - t' \frac{dT_s}{dt}$$

and

$$t' = \frac{T_s - T_m}{\frac{dT_s}{dt}}$$

(c) If  $T_s = T_0 + a \sin \omega t$

$$T_m = T_0 + (\omega^2 t'^2 + 1)^{-1/2} a \sin(\omega t - \phi)$$

$$t' = \frac{\tan \phi}{\omega}$$

When the thermometer is in a pocket the problem is more complicated. It is, however, approximately correct to assume a single time-constant which is a function of the capacities of the thermometer and the pocket and of the heat-transfer coefficients between steam and pocket and between pocket and thermometer. The above expressions can then be applied.

One might question the practical value of the high accuracies available from calibrated resistance-thermometers when one considers the other relevant factors which intervene, such as thermal lag, instrument inaccuracies and so on, and it would have appeared possible to use calibrated thermocouples which would have been equally acceptable from the point of view of time lag and could, it is felt, have been calibrated as was done for the resistance elements.

The authors have made brief comments on the problems associated with steam temperature control. These are in general associated with time lags not in the measuring circuit but in the superheating and desuperheating circuits, which are much more serious and may vary from several minutes to upwards of a quarter of an hour. It is for this reason that measurements which will predict the coming change in steam temperature are now commonly employed. Such measurements as steam load on the boiler and gas heat content are two commonly employed 'disturbance' signals.

**Prof. W. Fishwick:** The authors suggest a distance of 30 diameters down the pipe as a safe distance from the junction to the thermometer pocket. I have experimented with liquids, not steam, and found a minimum of 10 diameters quite sufficient. This, I think, is recognized practice in the chemical industry. The reason is probably that when fluids mix at a junction they are very seldom flowing with the same velocity, and the turbulence is considerably greater than that due to temperature effects alone.

The authors' calculation considers the core of hot steam inside an annular stream of colder steam. In that case the surface area between the two is large, but if the streams meet at a Y-junction the surface separating the two blocks of steam will be smaller. The authors' calculations are then rather optimistic, but taking the point made above into account, I think their criterion is still very safe.

Could the authors indicate whether it is the temperature at the pipe centre or the average temperature which they are interested

\* Transactions of the American Society of Mechanical Engineers, 1949, 71, p. 121.

in? The resistance thermometer they have chosen is rather fragile but measures the temperature at the pipe centre. If a slightly more robust pattern with a length of, say, 2 in had been chosen, perhaps the reading would be nearer the average steam temperature.

I support Mr. Llewellyn Young's remarks about thermocouples, but would be cautious in advocating springs to hold the thermometer bulbs in place. We had considerable trouble with jamming some years ago and abandoned them, but there may be better designs now.

**Mr. H. S. Horsman:** Some years ago Messrs. Buckland and Stack published their well-known paper\* and drew attention to some of the possible causes of error, and in doing this they succeeded in arousing a good deal of interest in the finesse of the measurement. Having aroused this interest, they confined their discourse to the errors associated with the inside of the pocket and of the thermometer or element itself. This caused a desire to know a good deal more about the various sources of error and, I am pleased to say, the present paper goes a long way towards satisfying this demand.

It was in 1948 that I was asked to suggest subjects for special investigation, and the present subject was one of those included in the list. For my part, I had in mind the 1050° F steam temperature measurements which were hypothetical eight years ago but which are very real to-day. It could be foreseen that owing to metallurgical limitations, fine control of temperature and therefore of measurement would be important.

Buckland and Stack were certainly among the first to insert the temperature detector directly into the steam, although the pressures they encountered were modest at 200 lb/in<sup>2</sup>. The authors have been successful in using directly exposed resistance elements at a pressure of 400 lb/in<sup>2</sup> at 775° F. For future work the element will have to withstand very high steam pressure with temperatures up to 1100° F. This would appear to rule out the use of the resistance element. The solution seems to me to lie in the direction of a directly exposed thermocouple, and, in fact, considerable success has recently been achieved in the application of such elements.

Reference to Fig. 14 shows a method of mounting thermometers which apparently was satisfactory for short-time use, but in dealing with more onerous conditions any design involving screw threads has been avoided. At very high temperature fine threads could become seized, and in view of the expendable nature of the elements they require to be demountable at any time. This requirement has led to the development of a special securing clamp which seems to be satisfactory for securing elements into pipes working at steam pressures much in excess of those in use at present.

I would draw attention to Section 5, where it is indicated that 'lag error' is likely to cancel out; I consider that the temperature lag has an importance which needs stressing in the case of 'once-through' and supercritical boiler plant. The rate of feed to the boiler will be controlled mainly by steam temperature, and for these exacting conditions direct-contact elements are a necessity, and it is a matter for consideration as to the thermo-electric material to be employed. The installation of the elements need cause no doubt, but there is some apprehension regarding the risk of contamination of the thermocouple material. Further work is needed so as to establish confidence in this very important subject.

**Mr. A. F. Brittin:** In co-operation with the C.E.A., investigations have been made into temperature measurement and temperature distribution across a pipe. The results will not materially diverge from the authors' conclusions, except regard-

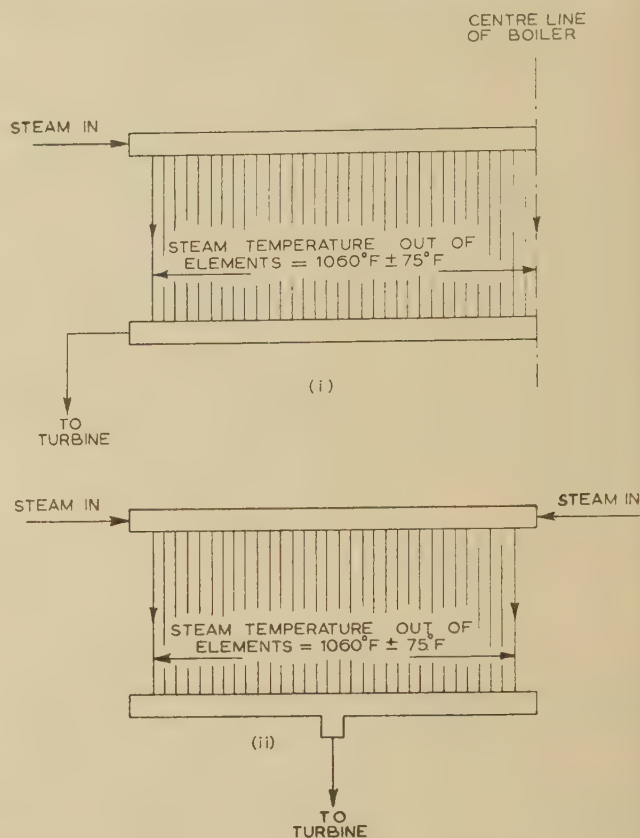


Fig. A

ing the magnitude of the time lag inherent in measurement employing thermometer pockets (see Fig. A).

At 1550 lb/in<sup>2</sup> and 1060° F, thermometer pockets have failed owing to their long immersion into the steam pipe. I advocate thermocouples directly immersed into steam for increased accuracy with rapid temperature changes and to reduce immersion length. Such couples are installed in a boiler operating at 1550 lb/in<sup>2</sup> and 1060° F.

Contrary to the authors' conclusions, time lag is important when measuring temperatures of 1000–1200° F, since on plant designed for high temperatures and pressures only a limited margin of temperature can be allowed on the materials of the superheater, piping and turbine. The authors' argument that the temperature of the steam pipe lags behind that of the steam does not apply to the superheater tubes. Minimum time lag is essential if rapid starting of modern plant under two-shift conditions is envisaged.

Resistance elements are unsuitable for continuous measurement of steam temperatures of 1000–1200° F. Further consideration should be given to the artificial ageing of thermocouples, choice of thermocouple materials and calibration of couples and instruments for measuring high steam temperatures.

Could the authors say, first, what allowance was made for errors due to close proximity of the various resistance elements used in measuring distribution of temperature across the pipe, and secondly, what should be the minimum distance between superheater header and temperature measuring point to give an accurate steam temperature, remembering that in severe cases of unequal firing and unequal slagging differences of the range 100–150° F can occur in the temperature of steam delivered to the header by individual superheater elements.

**Mr. F. Shakeshaft:** With the rapid increase in installation of

\* BUCKLAND, B. O., and STACK, S. S.: 'Thermocouples for Testing Steam Turbines', Temperature Measurement Symposium (New York, 1939).

large output-capacity plant to operate at high pressures and temperatures, the authors have made a valuable contribution to the subject of accurate measurement of temperature. This work is important to ensure high operating efficiencies and the safe use of large plants.

At some of these stations some 2½ to 3 million tons of coal will be consumed per annum—the cost of which will be around £10 million sterling. A 1% saving of fuel would save £100 000 per annum. These plants will have to be run under continuous test conditions if such financial savings and conservation of the nation's fuel resources are to be achieved.

On reheat units the steam temperatures at the stop valve and after resuperheating will have to be recorded with a minimum deviation from their absolute values which demands instrumentation of robust design and inherent accuracy.

To safeguard the plant it has been recommended that under normal conditions of operation the average steam temperature at the turbine stop valve over an operating period of twelve months should not exceed the rated temperature, and in maintaining this average the temperature should not exceed the rated temperature by more than 15° F. This applies even to stop-valve temperatures of 1 000 or 1 050° F.

Further, during abnormal conditions the temperature should not exceed the rated temperature ( $a$ ) by more than 25° F for operating periods aggregating not more than 400 hours per annum, and ( $b$ ) by more than 50° F for fluctuations lasting 15 min or less, aggregating not more than 80 hours per annum.

The above conditions demand accuracy from the instrumentation and also great skill in operation of the combined boiler and turbine plant.

It has been suggested that if conditions ( $a$ ) and ( $b$ ) were to occur in a restricted period instead of being evenly spread out over the year, the mechanical safety of the plant might be endangered. In these circumstances it would be most useful to have a reliable indication of the rate of change of temperature with time made available to the plant operators to minimize these swings.

This principle has already been developed by measuring temperatures with two thermocouples, one in direct contact with the steam and the other shielded from the steam. These thermocouples are connected in opposition and when the temperature is constant there is no output; when it is changing the output from the shielded thermocouple lags that of the direct thermocouple and causes a difference in output which is proportional to the rate of change of temperature. By the use of an electronic recording apparatus a high degree of sensitivity is obtained.

In conclusion, work has recently been carried out in the measuring of both steam and metal temperatures so that transient thermal stresses in the metal might be estimated under starting or rapidly changing load conditions on high-temperature plant—all of which calls for a high degree of accuracy.

**Mr. J. McMillan:** Like many others, the authors have given methods of calculation and data for 'conduction error' and 'radiation error' in each case assuming the absence of the other. The static error should be calculated from the basic overall heat balance, namely:

$$\begin{aligned} & \text{(Heat transferred from steam to temperature-detecting installation)} \\ &= \text{(Heat lost by conduction)} + \text{(Heat lost by radiation)} * \dagger \end{aligned}$$

## THE AUTHORS' REPLY TO THE ABOVE DISCUSSION

**Messrs. D. H. Lucas and M. E. Peelow (in reply):** We used resistance thermometers, not because we considered they should be used in the routine measurement of steam temperature, but because they would give a higher degree of accuracy than thermo-

Using the authors' nomenclature and method of presenting the error ( $\delta T$ ) in terms of the steam/pipe-wall differential, the true expression for a non-radiating gas or vapour is

$$\delta T = \frac{h_s}{h_s + h_r} \left\{ \frac{h_r}{h_s} + \frac{1}{\cosh \sqrt{\left[ \frac{\pi d (h_s + h_r)}{ak} \right] l}} \right\} \Delta T$$

as compared with the expression given by the authors' method:

$$\delta T = \left[ \frac{h_r}{h_s} + \frac{1}{\cosh \sqrt{\left( \frac{\pi d h_s}{ak} \right) l}} \right] \Delta T$$

The two expressions coincide only when  $h_s \gg h_r$ , which is not always the case at high temperatures, and for given values of  $h_s$  and  $h_r$  the discrepancy will increase with decreasing length of immersion,  $l$ .

Turning now to dynamic error, I suggest that in many cases it is serious, although its seriousness is not always apparent until automatic control is applied. The benefit to be derived from metallic contact between element and pocket depends on the width of the alternative air annulus. An air-gap of above, say,  $\frac{1}{16}$  in may have a significant influence on the lag and dynamic error. As the authors state, the dynamic error depends upon the thermal capacities of the element and pocket, the thermal resistance between steam and pocket, and between pocket and element. This implies a system with two time-constants—not one, as suggested by the authors—and is very important from a control point of view.\* I know of a case in which the steam temperature (exit boiler superheater) was very much improved by replacing the existing thermometer installation by one in which there was good pocket-element contact.

**Mr. H. J. Lowe:** I am tempted to challenge the accuracy attributed to thermocouples in Section 6.2 and to think the authors must have in mind alloy couples, such as chromel/alumel which—although they may be calibrated to within about 0.1%—are reliable only to about 0.75%. This arises from inhomogeneity effects where thermal gradients exist along the wires and cannot be eliminated by calibration, because the thermal gradients experienced in use cannot be reproduced with sufficient accuracy during calibration.

I believe that the platinum/platinum-rhodium couple would be more satisfactory in this respect, but probably multiple couples would be required to compensate for lower sensitivity, and the cost may well be prohibitive. Would the authors care to comment on the suitability of silver/palladium couples, which appear to have a satisfactory sensitivity in this temperature range?

Finally, with reference to Section 6.1, the steam velocity of 65 ft/sec, quoted for Deptford West, would be uncommonly low for high-pressure installations, and double this figure might be more typical. This would have a maximum temperature equivalent in the region of 0.7° F and, if one assumes a recovery factor of about one-half, a probable error of 0.35° F. This is rather greater than the paper suggests at first sight, although it is still not serious.

couples for the limited time covered by the experiment. We knew that the resistance thermometers we were using would not have a long life. We do not agree that calibrated base-metal thermocouples could have given an equal degree of accuracy. The

\* CICHETTI, M. T.: *Industrial and Engineering Chemistry*, 1948, 40, p. 1032.  
† WEST, W. E., and WESTWATER, J. W.: *ibid.*, 1953, 45, p. 2152.

\* AIKMAN, A. R., McMILLAN, J., and MORRISON, A. W.: *Transactions of the Society of Instrument Technology*, 1953, 5, p. 138.

calibration of a thermocouple can only be relied on if the temperature distribution in the thermocouple metal is the same during the test conditions as during the calibration conditions, and this, in general, is not the case.

Several speakers have stressed the need for high speed of response and for the direct immersion of thermometers in the steam. It is true that our work has shown that the error due to time lag is normally likely to be greater than the other errors, but we doubt the necessity for very much higher speeds, since all the other parts of the system have much longer time-constants than the conventional thermometer pocket. This may not be true for the superheater, but we think that the steam temperature in the main steam pipe is a very poor guide to that of the superheater metal, since this depends also on the speed of the steam, the speed and temperature of the flue gases and the cleanliness of the superheater tubes. Mr. Brittin has pointed out that the steam temperature in the superheater elements can vary by  $\pm 75^\circ\text{F}$ . We also doubt the advisability of direct immersion, since the increased accuracy due to higher speed of response may be offset by the increased difficulty of checking the calibration of the immersed thermocouples during service. However, if a higher speed is required, a considerable improvement can be made without resorting to direct immersion. If a  $\frac{1}{8}$ -in-diameter thermocouple is placed in a reasonably close-fitting pocket with just sufficient clearance to prevent seizure, the time-constant of the pocket system can be reduced to about 10 sec, which means that, even with extreme rates of change of temperature as high as  $15^\circ\text{F}$  per min, the maximum error would be  $2.5^\circ\text{F}$ . Such small pockets can be used without sacrificing mechanical reliability.

We have been interested for some time in silver-palladium thermocouples, which have been recommended electrically by the National Physical Laboratory but have a poor performance mechanically. We have hopes that the mechanical limitations can be overcome.

In reply to Mr. Llewellyn Young, we would agree there is every advantage in making the thermometer a close fit in the pocket, provided that there is little danger of seizure. We doubt

whether there is much to be gained by springs and other gadgets.

In reply to Prof. Fishwick, we are interested in the average temperature of the steam and not the temperature at the centre of the pipe, but under normal conditions the difference is negligible. We were interested in his figure of ten diameters for adequate mixing, but believe that it may be optimistic under certain conditions. The British Hydro-mechanics Research Association have recently issued a report\* which shows that the length required may vary from seven diameters when conditions are favourable to 100 diameters when the second stream is introduced axially at the centre of the pipe.

In reply to Mr. Brittin, the study of the effect of the many elements of the superheater on the outgoing steam is a complex statistical problem which we have not attempted. However, in the worst case, with half of the superheater producing steam  $75^\circ\text{F}$  above the average and the other half producing steam  $75^\circ\text{F}$  below the average, the situation approximates to the join of two pipes which we have dealt with. It would appear, then, to be desirable to make measurements 30 diameters after the various streams of steam have joined, and we should imagine this is not inconvenient in most cases.

The proximity of the different measuring elements would have an effect on the heat transfer from the steam to the thermometers, but the error, increased for this reason, would still be extremely small with our experimental arrangement.

In reply to Mr. McMillan, we agree that if the steam did not radiate his form would be more correct. However, the steam does radiate and the correct expression, considering correction and radiation effects together, is

$$\delta T = \left\{ \frac{0.4h_r}{h_s + h_r} + \frac{h_s + 0.6h_r}{h_s + h_r} \frac{1}{\cosh \sqrt{\left[ \frac{\pi d(h_s + h_r)}{ak} \right] l}} \right\} \Delta T$$

The difference between this and the expressions given in the paper will normally be negligible.

\* WHITEMAN, K. J.: 'The Diffusion of Matter in Turbulent Pipe Flow,' British Hydro-mechanics Research Association, Publication No. TN 505.

POWER STATIONS AND THEIR EQUIPMENT

A Review of Progress.

By V. A. PASK, C.B.E., M.I.Mech.E., Member.

(1) ELECTRICITY SUPPLY AND PERFORMANCE

(1.1) Generation and Demand

Table 1 analyses the increases in the amount of electricity generated in, the maximum demand on, the installed capacity of and the number of stations in the British Electricity Authority's areas (excluding the North of Scotland) in the ten post-war years, and includes the figures for the immediate pre-war year for com-

involved load shedding, load spreading and voltage control, to keep down the maximum demand, but these difficulties now appear to have been overcome by the combined efforts of the Authority and the manufacturers to make more generating plant available.

For comparison, the increases in generation in several other countries are given in Table 2, from which it will be seen that the recession of 1953 was fairly general throughout Europe, but that

Table 1

ANALYSIS OF TOTAL GENERATION,\* MAXIMUM DEMAND AND INSTALLED CAPACITY IN THE B.E.A.†

Year	Energy generated	Increase over previous year		Maximum demand	Increase over previous year		Installed capacity	Increase over previous year		Number of stations
1937-38	MWh × 10 <sup>3</sup> 22 372	MWh × 10 <sup>3</sup>	%	MW 6 434	MW	%	MW 8 695	MW	%	235
1944-45	37 278			8 828			11 860			
1951-52	58 499			12 595			15 769			
1952-53	60 752	2 253	3·83	13 547	952	7·57	17 157	1 388	8·80	209
1953-54	64 180	3 428	5·48	15 430	1 883	13·90	18 647	1 490	8·70	216
1954-55	71 330	7 150	11·15	16 612	1 182	7·67	20 184	1 537	8·35	215
Increase in 3 years		12 831	22·0		4 017	32·0		4 415	28·1	
Increase in 10 years		34 052	91·3		7 784	88·3		8 324	70·3	

\* Corrected to delete traction stations included in 1939 Report.  
† Excluding North of Scotland district.

Table 2

INCREASE IN OUTPUT OF ELECTRICITY IN VARIOUS COUNTRIES DURING THE PAST THREE YEARS

Year	United States			Western Germany			Canada			France			Italy		
	Output		Increase over previous year	Output		Increase over previous year	Output		Increase over previous year	Output		Increase over previous year	Output		Increase over previous year
	MWh × 10 <sup>3</sup>	MWh × 10 <sup>3</sup>	%	MWh × 10 <sup>3</sup>	MWh × 10 <sup>3</sup>	%	MWh × 10 <sup>3</sup>	MWh × 10 <sup>3</sup>	%	MWh × 10 <sup>3</sup>	MWh × 10 <sup>3</sup>	%	MWh × 10 <sup>3</sup>	MWh × 10 <sup>3</sup>	%
1951	370 673			33 932			54 852			38 282			29 223		
1952	399 224	28 551	7.70	37 284	3 352	9.9	59 409	4 557	8.32	40 740	2 458	6.42	30 843	1 620	5.55
1953	442 285	43 061	10.78	39 726	2 442	6.55	65 490	6 081	10.25	41 531	791	0.195	32 619	1 776	5.26
1954	472 215*	29 930	6.75	45 185	5 459	13.75	69 226*	3 736	5.76	44 500*	2 969	7.15	34 630*	2 011	6.17
Increase over 3 years		101 542	27.4		11 253	33.2		14 374	26.2		6 218	16.3		5 407	18.5

\* Provisional figure.

NOTES: *United States.* Public utilities only; source: Edison Electric Institute Statistics.  
*Germany.* Federal German Republic; public supply (including auto-producers' supplies to network); source: V.D.E.W.  
*Canada.* Public utilities only; source: Dominion Bureau of Statistics.  
*France.* Total production (including auto-producers); source: O.E.E.C.  
*Italy.* Total production (including auto-producers); source: O.E.E.C.

parison. Some idea of the technical advances which have been made may be gauged from the fact that, over the period 1944-55, 70.3% more plant has generated 91.3% more energy. Moreover, during the first half of this decade serious plant shortages

this year was something of a boom year in North America. The percentage increase over the past three years places Britain roughly mid-way between North America and France and Italy, and all are well below Western Germany, which to-day is generating almost as much electricity as did the whole of Germany in the period 1934-37.

### (1.2) Installed Plant Capacity

The Government's serious restrictions on the amount of generating capacity sanctioned to be installed in the war and post-war periods, together with material and man-power shortages, have resulted in the proportion of the plant more than 20 years old and still in commission rising from 3.1% in 1939 to 17.3% in 1946, and to 27.5% in 1954; in fact, about 90% of the generating plant in use in 1937-38 is still in service.

The amount of plant constructed during the war was confined to that essential to war requirements, and it had to be manufactured to existing designs. This restriction was imposed in the full knowledge that serious plant shortages would occur in the years following the war.

The boiler problem was difficult. High-head, straight-tube and header types were available, but the radiant-type single- or 2-drum bent-tube types had not been manufactured. It was agreed, and experience confirmed, that such boilers could be made in quantity at economic prices for a 900 lb/in<sup>2</sup> pressure cycle, whereas high costs and reduced manufacturing capacity would have been incurred by an insistence on 1200 lb/in<sup>2</sup> or higher pressure cycles. In these circumstances a statutory order was issued restricting the building of turbo-alternators to 30 and 60 MW units only, for 600 and 900 lb/in<sup>2</sup> pressure cycles respectively, to expedite production with a view to overcoming the serious plant shortage.

The first post-war programme (1946-52 inclusive), for 8 434 MW of plant, comprised 92.4% of standard designs, of which 600 and 900 lb/in<sup>2</sup> pressure cycles formed 86.3%. To promote technical progress, even at a time of material and man-power shortage, 7.6% of the plant was ordered under special licence for operation at higher pressures and temperatures.

The second programme, for 1953-59 and covering 9 946 MW of plant, includes 85.2% for steam cycles at 900 lb/in<sup>2</sup> or higher, of which 31.2% is for 1 350, 1 500 and 2 350 lb/in<sup>2</sup> cycles, with a view to being an intermediate step in the improvement of thermal generation. Also included in this programme are a number of large reheat machines for the higher pressure cycles.

Further programmes will comprise three sizes of reheat set built for operation under the higher pressure and temperature cycles. Such plant will have to be designed to operate under 2-shift conditions, since it will in due course have to work in conjunction with large base-load nuclear generating plant.

### (1.3) Thermal Efficiencies

The thermal efficiency of steam generating stations in both Britain and the United States remained almost static for the decade 1940-49, so that improvements have been restricted to the last five years. In this country, improvements in thermal efficiency since 1938 have been adversely affected by three main factors. First, as previously mentioned, the almost tenfold increase in the proportion of plant more than 20 years old. Secondly, the necessity for keeping increasing amounts of the older lower-pressure plant in operation for longer running hours, which, combined with a deterioration in the quality of the fuels reaching the generating stations, has caused an increasing fraction of the total supply to be generated at low efficiencies. Thirdly, to overcome a serious plant shortage in a minimum of time, about 7 250 MW—86.3%—of the capacity included in the first 7-year plant programme was confined to either existing designs or mainly to standard 30 and 60 MW sets operating under 600 or 900 lb/in<sup>2</sup> steam cycles. Experience has confirmed that these steps were fully justified, for the actual material and man-power shortages caused the programme to run two years late.

Since a major proportion of the plant has been commissioned in the last four years, and the 900 lb/in<sup>2</sup> plant in the last three

years, improvements in thermal efficiency are mainly confined to the latter period. The highest thermal efficiency for 1954 is 13.3% better than that for 1937, but will be materially increased in the near future when high-pressure, high-temperature and reheat plant is commissioned. For the best 20 and 50 stations and the whole of the plant the corresponding improvements are 17.5, 20.8 and 17.4% respectively.

Table 3 shows in greater detail the performance of the first 20 and 50 stations, together with that for all stations. In 1954 many of the first 20 and 50 stations were only partially completed, which is why they sent out only 26.6 and 55.5% respectively of the total energy generated for the country, compared with 45.8 and 77.8% in 1937.

The second post-war programme, for 1953-59, provides for the installation of 9 947 MW of plant, of which about one-half will be standard 60 MW sets for 900 lb/in<sup>2</sup> pressure and one-third for pressures of 1 350, 1 500 and 2 350 lb/in<sup>2</sup>, fourteen of the latter being large reheat sets. It was again deemed necessary to use standard 60 MW sets, to avoid further serious plant shortages and give manufacturers time to develop designs and tool the shops for the production of large straight and reheat boilers and turbines. Future programmes will include only three sizes of reheat plant.

On the completion of this programme about 46% of the total plant installed in Britain will operate at 900 lb/in<sup>2</sup> or higher, as compared with 2.2% in 1949 and 23% in 1954. It is estimated that the thermal efficiency for the whole country will be about 27% and that the best station will exceed 35%.

The war and its aftermath caused stagnation for at least a decade; the improvements in thermal efficiency are therefore largely associated with the subsequent decade, and may well be 29% for the best station and over 33% for the whole country, compared with 38 and 34% respectively in 1929-39.

Improvements in the thermal efficiency of American generating stations have been more rapid than in the United Kingdom for a number of reasons. First, no Government restrictions, material or man-power shortages were encountered, and new plant designs and either new or reconstructed manufacturing works giving additional capacity became available from about 1949. Secondly, the combination of national load factors exceeding 60% (compared with 45% in the United Kingdom) and, in general, much cleaner fuels of higher calorific values permitted the continuous operation of large high-efficiency reheat sets and simpler boiler plant. Thirdly, a number of new large high-efficiency base-load stations (generating nearly as much energy as the whole of the United Kingdom in 1950) were required in association with atomic diffusion and other plants. Fourthly, the annual increase of capacity ordered has been from four to nearly nine times that permitted in the United Kingdom, the peak occurring in 1950-51, when 17 500 MW, or 30.5% of the total capacity then installed, was ordered to meet the increased national activity anticipated in the industrial and atomic fields. This plant, mainly comprising large sets, is being supplied by fewer than half the number of manufacturers making equipment for British stations, so that effective rationalization of design and production is much simpler. Finally, consents for construction are readily obtained and plant can be commissioned in 3-4 years from the planning date; operating experience is therefore available some 2-3 years earlier than is possible in this country.

The large amount of reheat generating plant commissioned between 1949 and 1954 resulted in an average improvement of 22% in the thermal efficiency of the generating stations.

### (2) STEAM CONDITIONS

In a maintained drive to improve thermal efficiencies, operating steam pressures and temperatures have continued to rise; these

Table 3

FIFTY STATIONS WITH HIGHEST THERMAL EFFICIENCY FOR 1952, 1953 AND 1954

## PASK: POWER STATIONS AND THEIR EQUIPMENT

171

1954

1953

1952

Station or section	Pres- sure	Division	Energy sent out	Thermal efficiency	Station or section	Pres- sure	Division	Energy sent out	Thermal efficiency	Station or section	Pres- sure	Division	Energy sent out	Thermal efficiency
Littlebrook "B"	1235	S.E.	812 177	29.95	Portobello H.P.	1350	S.E.S.	451 233	30.86	Portobello H.P.	1350	S.E.S.	505 825	31.27
Dunston "B" II	600R	N.E.	178 103	29.65	Stourport "B"	1250	M.	366 075	30.40	Stourport "B" L.P.	1250	M.	453 378	30.78
Portobello H.P.	1350	S.E.S.	131 167	29.46	Dunston "B" II	600R	N.E.	750 612	30.47	Littlebrook "B"	1235	S.E.	753 429	30.39
Skelton Grange	900	Y.	763 157	29.19	Littlebrook "B"	1235	S.E.	749 288	29.68	Dunston "B" II	600R	N.E.	722 061	29.89
Bromborough	900	M. & N.W.a.	446 366	28.88	Bromborough	900	M. & N.W.a.	1104 199	29.10	Bromborough	900	Y.	1320 061	29.23
Stourport "B"	1250	M.	241 746	28.84	Skelton Grange	900	Y.	1191 241	28.76	Skelton Grange	900	Y.	1428 258	28.98
Battersea "B" H.P.	1350	S.W.S.	538 165	28.71	Brunswick Wharf	900	S.W.S.	647 976	28.62	North Tees "C"	900	N.E.	180 857	28.98
Poole	900	S.	965 827	28.31	Brunswick Wharf	900	N.E.	1032 952	28.61	Brunswick Wharf	900	L.	1060 941	28.71
Braehad	900	S.W.S.	805 631	27.79	North Tees "C"	900	L.	898 444	28.53	Uskmouth	900	S.	1160 635	28.57
Agcroft H.P.	600	N.W.	571 020	27.20	Battersea "A"	1350	S.W.a.	635 301	28.24	Uskmouth	900	S.	1291 990	28.50
Blackwall Point	600	L.	284 884	26.66	Uskmouth "A"	900	S.W.a.	408 883	28.21	Brighton "B"	900	S.E.	700 363	28.37
Clyde's Mill H.P.	600	S.W.S.	317 798	26.66	Huncoat	900	N.W.	428 780	27.46	Bradbrook "C"	900	S.W.S.	1249 598	28.37
Fulham	600	L.	2044 607	26.63	Poole	900	S.	1109 105	27.37	Bradbrook "C"	900	N.W.	706 302	27.98
Cliff Quay	600	E.	368 561	26.47	Agcroft H.P.	900	N.W.	548 646	26.71	Keaby	900	Y.	556 801	27.87
Stuart Street H.P.	900	N.W.	242 751	26.41	Staythorpe	900	E.M.	874 686	26.63	Keaby	900	L.	617 416	27.84
Brimdown "B" H.P.	1900	E.	1825 000	26.24	Blackwall Point	900	Y.	588 622	26.61	Huncoat	900	N.W. & M.	427 892	27.75
Hams Hall "B"	650	S.W.a.	752 144	26.23	Keaby	900	L.	302 413	26.52	Battersea "B"	1350	L.	1004 450	27.71
Lynfi	600	E.M.	764 249	26.18	Stuart Street H.P.	900	E.	1473 589	26.49	Carmarthen Bay	900	S.W.a.	1757 819	27.70
Staythorpe	600	E.M.	698 864	26.02	Cliff Quay	600	E.	324 429	26.42	Agcroft H.P.	600	N.W. & M.	627 757	27.06
Croydon	600	S.E.	14 637 419	27.31	Clyde's Mill H.P.	600	S.W.S.	14 210 040	28.10	Agcroft H.P.	600	N.W. & M.	17 630 333	28.64
(a) Total of above 20 stations	..	..	..	..	..	..	..	..	..	..	..	..	..	..
Nechells "B"	600	M.	546 023	25.70	Fulham	600	L.	2 051 003	26.38	Clyde's Mill H.P.	600	S.W.S.	345 956	26.95
Plymouth "B"	600	S.W.	176 217	25.69	Croydon "B"	600	S.E.	682 053	26.19	Staythorpe "A"	900	E.M.	1279 152	26.94
Meaford "A"	600	M.	811 322	25.62	Brimdown "B" H.P.	1900	E.	241 562	26.13	Roosecote	600	N.W. & M.	287 037	26.90
Kingston	600	S.E.	588 556	25.42	Hams Hall "B"	650	M.	1 565 448	26.10	Blackwall Point	900	L.	306 108	26.74
Earley	615	E.	497 244	25.38	Lynfi	600	S.W.a.	732 675	26.03	Chadderton "B"	900	N.W. & M.	348 054	26.54
Keasley H.P.	600	N.W.	1 438 469	25.36	Meaford	600	M.	834 594	25.93	Stuart Street H.P.	600	N.W. & M.	408 585	26.45
Little Barford	650	E.	384 826	25.26	Plymouth "B"	600	S.W.	420 711	25.81	Cliff Quay	600	E.	1547 880	26.42
Barking "B"	600	L.	1754 380	25.21	Neuchells "B"	600	M.	848 359	25.63	Fulham	600	I.	1792 929	26.42
Dunston "B" I	600	N.E.	1182 323	25.21	Little Barford	650	N.E.	370 394	25.34	Croydon "B"	600	S.E.	836 228	26.27
Trafford	400	N.W.	353 846	25.07	Dunston "B" I	600R	N.E.	1 083 732	24.95	Hams Hall "B"	650	M.	1 587 098	26.21
Leicester H.P.	600	E.M.	362 228	25.00	Barking "B"	600	L.	1 489 451	24.94	Plymouth "B"	600	S.W.	429 810	26.06
Brimdown "A"	1900	E.	193 069	24.91	Earley	615	S.	445 037	24.90	Brimdown "B" H.P.	1900	E.	125 466	25.96
Rotherham	600	Y.	769 124	24.82	Walsall	600	M.	630 614	24.90	Connah's Quay	600	N.W. & M.	174 920	25.93
Battersea "A" L.P.	600	L.	1 062 699	24.79	Newport H.P.	600	S.W.a.	369 430	24.83	Meaford "A"	600	M.	837 020	25.82
Taylor's Lane H.P.	1300	L.	238 106	24.71	Battersea "A"	600	L.	1 048 325	24.83	Little Barford "A"	650	S.W.a.	763 367	25.77
Stockport H.P.	600	N.W.	134 245	24.71	Brimdown "A"	1900	E.	190 638	24.80	Neuchells "B"	600	M.	348 582	25.76
Portsmouth H.P.	600	S.	440 391	24.62	Trafford	400	N.W.	370 753	24.76	Huddersfield H.P.	600	Y.	1197 991	25.65
Newport H.P.	600	S.W.a.	372 048	24.56	Leicester H.P.	600	E.M.	130 911	24.72	East Yelland	600	S.W.	174 676	25.59
Mexborough	600	S.	361 284	24.25	Stockport H.P.	600	L.	403 092	24.72	Kearsley H.P.	600	N.W. & M.	313 338	25.23
Littlebrook "A"	600	S.E.	593 354	24.25	West Ham "B"	600	Y.	832 058	24.65	Rye House	600	E.M.	943 806	25.23
Rye House	600	E.	133 270	24.17	Rotherham	600	E.	222 161	24.56	Stockport H.P.	600	N.W. & M.	118 803	25.05
Kirkstall H.P.	600	Y.	347 339	24.17	Rye House	600	E.	457 211	24.55	Bold "A"	600	N.W. & M.	136 737	25.04
Barking "C"	900	L.	73 405	24.08	Kingston "B"	600	S.E.	1136 731	24.43	West Ham "B"	600	L.	244 281	24.92
Walsall	600	M.	544 355	24.08	Clarence Dock H.P.	1300	M. & N.W.a.	212 129	24.29	Doncaster "B"	600	Y.	447 584	24.86
Norwich H.P.	600	E.M.	116 694	23.93	Taylor's Lane H.P.	600	S.	497 248	24.26	Leicester H.P.	600	E.M.	369 755	24.83
Nottingham	600	E.M.	1199 489	23.93	Portsmouth H.P.	600	S.	1309 610	24.20	Clarence Dock H.P.	600	L.	1307 431	24.83
Castle Meads	600	S.W.	182 365	23.82	Nottingham	600	E.M.	497 248	24.20	Battersea "A"	1900	E.	912 219	24.79
Harthead H.P.	620	N.W.	434 605	23.54	Littlebrook "A"	600	S.E.	546 442	24.16	Brimdown "A"	600	E.	174 297	24.74
Clarence Dock H.P.	600	M. & N.W.a.	978 052	23.47	Upper Boat H.P.	500	S.W.a.	497 277	24.05	Walsall	600	M.	701 757	24.65
Ribble H.P.	600	N.W.	556 316	23.45	..	..	..	..	..	..	..	..	..	..
(b) Total of above 50 stations	..	..	31 463 057	25.87	..	..	..	35 903 142	26.29	..	..	..	36 434 731	27.11
(c) Total of all steam stations	..	..	56 474 791	22.61	..	..	..	59 632 003	23.22	..	..	..	66 308 265	23.72
(a) as percentage of (c) ..	..	..	55.6	..	..	..	60.3	..	..	..	..	..	26.6	..
(b) as percentage of (c) ..	..	..	..	..	..	..	..	..	..	..	..	..	55.5	..

N.B.—The list excludes stations or sections which did not come into commercial operation until after 1st January.

increases, particularly those of pressure, usually occur in definite steps, which are, for a time, standardized.

A review of plant development for use at selected and interconnected generating stations from 1926 to 1939 indicates that between 1926 and 1932 standard pressures increased from 300 to 400 lb/in<sup>2</sup> by 50 lb/in<sup>2</sup> increments only, mainly limited by boiler development. Turbines of 50 MW capacity operating at 1500 r.p.m. and provided with feed trains for preheating the condensate to about 300° F were installed at new or reconstructed selected generating stations.

Between 1933 and 1939 the standard pressure was increased to 600 lb/in<sup>2</sup>, and 1500 r.p.m. turbines of 60–105 MW capacity were built with single- and double-flow exhausts respectively, the final temperature from the feed-heating trains being about 340° F. These large slow-speed sets were installed in new selected generating stations.

A number of 3000 r.p.m. 30 MW sets for up to 600 lb/in<sup>2</sup> were installed at the smaller selected and interconnected generating stations. In addition, a limited number of experimental plants designed for 1250–1900 lb/in<sup>2</sup>, the latter with reheat, were included in these programmes.

During the planning of the 1946–52 programme investigation showed that a 3000 r.p.m. turbine frame could be developed using a double-flow low-pressure turbine casing for output capacities of up to 50 MW at 600 lb/in<sup>2</sup>, or 60 MW at 900 lb/in<sup>2</sup>. Moreover, this development would inaugurate a new range of 3000 r.p.m. high-pressure high-temperature large-capacity double- or triple-flow turbines as soon as large boilers became available.

It was appreciated that many of these 50 and 60 MW 3000 r.p.m. sets would in the future be relegated to block loading under 2-shift operation and would be more suitable for this duty than the heavy 50, 60 and 100 MW 1500 r.p.m. sets. Designs for 3000 r.p.m., first experimental 60 MW, and later 100 MW, sets were also developed for operating on a straight condensing cycle with stop-valve conditions of 1500 lb/in<sup>2</sup> and 1050° F. The factors governing the choice of this pressure were that existing manufacturing plant was available to produce the boiler drums, and that it would provide room for another step to 2350 lb/in<sup>2</sup> at some future date.

The development of radiant boiler designs for 505 000 and 830 000 lb/h evaporation, in addition to the existing 300 000 lb/h size, enabled 30, 60 and 100 MW straight condensing sets to be economically arranged on the unit principle for 600, 900 and 1500 lb/in<sup>2</sup> cycles.

Prior to the planning of the 1953–59 programmes, an experimental 50 MW reheat tandem-compound 3-cylinder single-shaft machine and unit reheat boiler had been commissioned. The stop-valve conditions were 600 lb/in<sup>2</sup> at 850° F, with reheat at 135 lb/in<sup>2</sup> to 850° F and a final temperature of 340° F from the feed train. The experience gained with this plant confirmed data obtained in the United States that high efficiency and simplicity of operation, combined with flexibility and reliability under service conditions, justified the adoption of the reheat cycle for 100–120 MW high-pressure plant, even though such sets might in the future be relegated to 2-shift operation.

The first three trial reheat sets were 3000 r.p.m. 100 MW tandem-compound 3-cylinder single-shaft reaction machines, and by agreement with the manufacturers the initial and reheat temperatures were limited to 970 and 950° F at 1500 and 400 lb/in<sup>2</sup> respectively. A new standard was later evolved for a 3000 r.p.m. 120 MW tandem-compound 3-cylinder single-shaft set by using the existing low-pressure double-flow cylinder of a standard 1500 lb/in<sup>2</sup> 100 MW straight condensing set. Stop-valve and reheat steam conditions were standardized at 1500 lb/in<sup>2</sup> at 1000° F and 400 lb/in<sup>2</sup> at 1000° F respectively, the 6-stage feed-heater train preheating the condensate to 435° F at

the discharge from the last heater. Unit reheat boilers of 860 000 lb/h capacity are used with the new reheat turbines.

Since the continued rapid rise in the national electricity demand is estimated to require some 28 000 MW of installed capacity by 1959, it was deemed advisable to consider the installation of a number of 200 MW reheat sets at generating stations on the coalfields, for exporting energy over considerable distances. This capacity warranted the adoption of the next logical pressure step—to 2350 lb/in<sup>2</sup>—at the turbine stop-valve. Investigation showed that 200 MW unit reheat boilers could be constructed with two separate furnaces for an evaporative capacity of about 1 400 000 lb/h, and that a 200 MW generator could be constructed with direct cooling of its rotor and stator bars. The use of a separate magnetic stator core in the generator frame has enabled the maximum weight to be transported to the site to be kept below the present permissible limit.

Cross-compound and tandem-compound turbine projects were examined, and the latter adopted. Three 3000 r.p.m. 200 MW reheat tandem-compound 3-cylinder triple-exhaust single-shaft machines and unit boilers have been ordered, the steam conditions being 2350 lb/in<sup>2</sup> and 1050° F at the stop valve with reheating at 475 lb/in<sup>2</sup> to 1000° F. On later sets the initial and reheat temperatures may be raised to 1100 and 1050° F respectively. A 6-stage feed-heater train preheats the condensate to 460° F at the discharge from the last heater. The first set will be commissioned in 1959.

Four 100 MW and nine 120 MW sets, together with one 200 MW set, all of the reheat type, are included in this programme.

In developing the principles of post-war standardization of large 3000 r.p.m. sets, steam pressures ranging from 600 to 2350 lb/in<sup>2</sup> and output capacities from 30 to 200 MW approximately constitute a geometric series with an average common ratio of about 1.6 between adjacent pressure steps and some of the capacity steps. This enables almost the same dimensions to be used for the first group of high-pressure stages of turbines blading operating as between a new pressure step and the previous pressure step, and also for main and/or reheat piping, governing and control-valve gear. These pressure and capacity steps closely coincide with American development but avoid a number of intermediate steps.

It was desirable that two sizes of high-speed last wheel should cover a wide range of output capacity when used in single-, double- or triple-flow arrangement, because the design and construction of the wheel and its blading, including aerodynamic flow tests, static and dynamic vibration tests of the blades, and reliability confirmation under service conditions may take five years. Two wheels, each capable of a 4 : 1 capacity ratio, or in combination a 7 : 1 ratio, have been developed to meet home and export requirements. The first covers from 30 MW in single flow on a 600 lb/in<sup>2</sup> cycle to 120 MW in triple flow on a 1500 lb/in<sup>2</sup> reheat cycle; and the second covers from 50 MW in single flow on a 900 lb/in<sup>2</sup> cycle to 200 MW in triple flow on a 2350 lb/in<sup>2</sup> reheat cycle.

Straight condensing turbines for 600, 900 and 1500 lb/in<sup>2</sup> and 825, 900 and 1050° F initial temperature have a moisture content at the discharge from the last low-pressure blade of about 12.5–13% at 29 in of vacuum—or about the same values as were used with safety on the early low-pressure sets. On a 1500 lb/in<sup>2</sup> reheat cycle the moisture content at the last low-pressure blade is reduced to about 8%, which will prolong its life materially. Since the reheat cycle can be justified on economic grounds for these high pressures, they have been adopted as standard practice on the unit reheat boilers now developed for 750 000, 860 000 and 1 400 000 lb/h evaporative capacity. The 3000 r.p.m. standards may be summarized in Table 4.

If the nation's coal resources are to be conserved, every effort

Table 4

STANDARD STEAM CONDITIONS, OUTPUT CAPACITIES AND AVERAGE TURBINE HEAT RATES AT 28.9 IN OF VACUUM

Steam cycle	Pressure	Temperature	Capacity	Heat rate	Exhaust flow	Saving in heat rate	
						600 lb/in <sup>2</sup> datum	900 lb/in <sup>2</sup> datum
	lb/in <sup>2</sup>	°F	MW	B.Th.U./Wh		%	%
Straight .. ..	600	850	30-50	10	Single or double	0	—
Straight .. ..	900	900	60	9.25	Double	7.5	0
Straight .. ..	1500	1050	100	8.5	Double or triple	15	8.1
Reheat .. ..	1500	1000/1000	120	8.2	Double or triple	18	11.4
Reheat .. ..	2350	1050/1000	200	7.7	Triple	23	16.7

must be made to use high-pressure and high-temperature plant and to make it reliable for future 2-shift operating conditions. Any further forward step in pressure and temperature would involve operation at pressures materially higher than the critical value for steam of 3200 lb/in<sup>2</sup>, or at about 4500-5000 lb/in<sup>2</sup>; no such plants are planned for construction in the United States and one in Germany.

The success of these projects depends on the development of alloy steels capable of withstanding the combined high pressures and temperatures, and also on developments in design, construction and refinement of details incorporated in turbines and boilers.

#### (2.1) American Developments

In the United States the first 3600 r.p.m. 100 MW 1250 lb/in<sup>2</sup> 975° F 28½-in-vacuum straight condensing 2-cylinder tandem-compound double-flow-exhaust single-shaft set with a unit boiler was installed in 1947 at the Essex station, New Jersey. At other stations, triple-flow-exhaust turbines for 29 in vacuum were installed with output capacities of up to 110 MW when operating on a 1500 lb/in<sup>2</sup> 1050° F straight condensing cycle.

Since the effect of reheating to 1000° F at 400-360 lb/in<sup>2</sup> is to increase the useful overall blading heat drop by 16-18% compared with that available under a straight condensing cycle, this new 3600 r.p.m. frame was uprated to 125 MW. The same exhaust wheels were used, for they pass about the same weight of steam per hour as the straight condensing set.

Cross-compound reheat sets, of 125, 150 and 200 MW capacity were also built, for initial steam conditions of 1800 lb/in<sup>2</sup> and 1000° F, with reheating at 450 lb/in<sup>2</sup> to 1000° F, with the larger sizes for 2000 lb/in<sup>2</sup> at 1050° F with reheat at 510 lb/in<sup>2</sup> to 1000° F. These cross-compound sets comprised a "topping type" 3600 r.p.m. single-cylinder high-pressure line expanding steam from stop-valve to reheat pressure, and an 1800 r.p.m. tandem-compound 2-cylinder double-flow-exhaust single-shaft low-pressure line expanding steam from 400 to 450 lb/in<sup>2</sup> and 1000° F to condenser pressure.

During 1946-50 some 43 reheat sets totalling about 3790 MW capacity were installed or ordered. They comprised three 1800 r.p.m. tandem-compound 80 MW sets for the Port Washington station, thirty-two 3600 r.p.m. tandem-compound sets totalling 2480 MW and eight 3600-1800 r.p.m. cross-compound sets totalling 1070 MW. In the peak year, 1950-51, some 17500 MW of plant were ordered, including 111 reheat sets totalling 12207 MW. These comprised ninety-five 3600 r.p.m. tandem-compound sets totalling 9942 MW, thirteen 3600-1800 r.p.m. cross-compound sets totalling 1785 MW, and three 3600-3600 r.p.m. cross-compound sets totalling 480 MW.

In succeeding years about 60-65% of the total plant ordered, i.e. about 5000 MW per annum, comprised large-capacity reheat sets, with a balance in favour of the 3600 r.p.m. units up to 200-250 MW, and 3600-1800 r.p.m. cross-compound units up

to 350 MW. All post-war reheat turbine plant will operate in conjunction with unit reheat boilers.

In 1951 the first orders were tentatively placed for 3600 r.p.m. tandem-compound 2- and 3-cylinder single-shaft reheat sets for capacities of 145-200 MW, some with turbine stop-valve conditions of 2350 lb/in<sup>2</sup> at 1100° F with reheating to 1050° F at 460-510 lb/in<sup>2</sup>.

The first 2350 lb/in<sup>2</sup> 1100° F, 3600 r.p.m. 145 MW tandem-compound machine was commissioned at the Kearny station, New Jersey, in 1953, and a 200 MW 1880 lb/in<sup>2</sup> 1050/1050° F tandem-compound set at the Kingston station, Tennessee, in late 1954. Further high-speed sets of 185, 200 and 250 MW capacity are being commissioned in 1955.

There is a definite trend among American manufacturers of both large tandem-compound and cross-compound reheat sets to use high-pressure casings containing two separate groups of blading with outward flow of steam to the exhaust branches provided at each end of the casing, the centrally located initial and reheat steam-admission branches to these back-to-back blading groups being separated by an internal diaphragm and gland. This ensures that the high-pressure rotor is in near thermal balance from its centre to the glands or bearings, that the blading thrusts are in fair balance and also that the shaft glands operate under greatly reduced pressures and temperatures. The steam conditions at the inlet to the intermediate cylinder of a large tandem-compound machine may be reduced by more than 200 lb/in<sup>2</sup> and 150-200° F, which contributes materially to the reliability of the whole set.

The application of this new construction to large cross-compound reheat units has eliminated the intermediate-pressure turbine and its large-diameter blading, subject to high temperatures and gradients from the 1800 r.p.m. low-pressure line, by including a few additional low-temperature stages in the 1800 r.p.m. low-pressure double-flow turbine. Advantages are that all high pressures and temperatures are confined to the 3600 r.p.m. turbine with shaft sealing glands working under low pressures and temperatures, and that the inlet to the 1800 r.p.m. blading is supplied at 300-400 lb/in<sup>2</sup> less pressure and 400-340° F lower temperature than in the old type of construction. Moreover, with a transverse arrangement of the turbine plant, 350 MW sets can now be installed in an engine room previously designed for 150 MW sets, because the new low-pressure line of the 350 MW set is no longer than the 2-cylinder low-pressure line of the original 150 MW sets.

Supercritical plant development for stop-valve conditions of 4500-5000 lb/in<sup>2</sup> and 1150-1200° F, with two stages of reheating, has now entered the construction stage. On the turbine side the only unknown steam zone is its expansion from 4500-5000 to 2300 lb/in<sup>2</sup>, where small heat drops over the turbine blading are associated with large pressure differences. This demands great skill in designing the blade path to prevent heavy interstage leakage and in solving problems associated with the cooling of

the rotor body in a very high temperature region until new and better alloy steels for rotor construction become available.

The boiler must be of the forced-flow once-through type for operation at supercritical pressures. Experimental data were required for the design of prototype boilers in respect of feed treatment, heat-transfer rates, pressure drops, and control and response characteristics, and pilot plants were built to obtain these data.

It is estimated that the heat rate of supercritical-cycle pressure plant will be about 7% lower than that of the most efficient subcritical-cycle plant now in operation, and the output capacity will be about 40% greater for the same steam flow to the turbine stop valve. Some of the main components of a 200 MW subcritical turbine can therefore be uprated to about 280 MW under supercritical 2-stage reheating conditions. Up to the present, three supercritical plants have been ordered, two in the United States and one in Germany.

Scheduled for commissioning in 1956, the 120 MW set at the Philo, Ohio, station will be installed in the space originally occupied by a 40 MW low-pressure unit. The cyclone-fired boiler will supply steam at 4500 lb/in<sup>2</sup> and 1150° F, the first reheat stage being at 1200 lb/in<sup>2</sup> to 1050° F and the second at 180 lb/in<sup>2</sup> to 1000° F. The 3600 r.p.m. tandem-compound 3-cylinder turbine will expand the steam from stop-valve to first-stage reheat pressure in the first cylinder; the second cylinder combines the first- and second-stage reheat groups of blading in a common casing, and the third cylinder is a conventional double-flow casing exhausting to the condenser at 1.5 in Hg absolute.

Scheduled for commissioning in 1959 is a 275 MW set at a new station to be built on the River Delaware. The 1500000 lb/h boiler will supply the turbine at 5000 lb/in<sup>2</sup> at 1150° F, the first reheat stage being at about 1100 lb/in<sup>2</sup> to 1050° F and the second at 225 lb/in<sup>2</sup> to 1050° F; the boiler will have two pulverized-fuel corner-fired furnaces. The tandem-compound 3600 r.p.m. 4-cylinder turbine will be of the triple-exhaust type; the first cylinder will expand steam from 5000 to 2400 lb/in<sup>2</sup>, the second will combine both high-pressure and first-reheat-pressure blade groups in a common casing, the third will combine second-reheat-pressure and single-flow low-pressure blade groups in one casing, and the fourth will be a conventional double-flow low-pressure casing exhausting to the condenser at 1.5 in Hg absolute.

Scheduled for commissioning late in 1956, the third of these plants is of about 120 MW capacity and is to supply a large chemical works in Germany. The forced-flow Benson boiler will be pulverized-fuel fired and will supply the turbine at 4500 lb/in<sup>2</sup> at about 1132° F. It will have two stages of reheating at pressures and temperatures closely resembling those of the American plant. The turbine plant will probably comprise a separate high-pressure line and a tandem-compound 3-cylinder low-pressure line. Steam will be extracted from the low-pressure line for supplying factory process steam, and dependent on the results obtained on a small pilot supercritical boiler, either steam transformers (evaporators) or demineralizing plant will be used. If steam transformers are necessary the whole plant will operate on a closed feed system, but if demineralizing plant is found suitable the process steam make-up will be treated before entry to the condenser.

### (3) TURBINE PLANT AND AUXILIARIES

#### (3.1) Size of Units

In Britain some 367 turbines with a total capacity of 18380 MW are to be installed in two 7-year periods; these comprise 196 turbines totalling 8434 MW in the 1946-52 programme, and 171 totalling 9946 MW in the 1953-59 programme. At the

end of 1954 some 202 turbines, totalling 8650 MW had been commissioned. The reinforcement of the existing Grid system with 275 kV sections has enabled larger and more efficient units to be adopted for the 1953-59 programme, when 24 machines (14 of which are of the reheat type) of 100-200 MW output form 27% of the capacity ordered. In subsequent years high-capacity reheat plant will form ever-increasing proportions of the total ordered.

About 97% of the total turbine plant has been built on seven frames, comprising four standard and three non-standard existing frames.

#### Standard Frames.

- (a) 117 turbines of 30-32 MW capacity for 600 lb/in<sup>2</sup> cycles.
- (b) 139 turbines of 60-61.6 MW capacity for 900-1500 lb/in<sup>2</sup> cycles.
- (c) 9 turbines of 120 MW capacity for 1500 lb/in<sup>2</sup> reheat cycles.
- (d) 1 turbine of 200 MW capacity for a 2350 lb/in<sup>2</sup> reheat cycle.

#### Non-Standard Frames.

- (a) 13 turbines of 40-45 MW capacity, for 400-600 lb/in<sup>2</sup> cycles.
- (b) 50 turbines of 50-53 MW capacity for 600-900 lb/in<sup>2</sup> cycles.
- (c) 15 turbines of 100 MW capacity operating with one exception on 1500 lb/in<sup>2</sup> cycles, four being of the reheat type.

There are in addition 23 turbines, mainly duplicates of old machines, 20 of which range from 12.5 to 20 MW capacity at 3000 r.p.m. and three of 75 MW capacity at 1500 r.p.m.

In the future, turbine plant will be built mainly on three frames, 60-80 MW reheat machines being installed at reconstructed stations, and 120 and 200 MW reheat machines at new stations.

The classification by generation capacity steam conditions at the turbine stop valve is shown in Table 5, from which it will be noted that 68% of the new plant ordered will operate at or above 900 lb/in<sup>2</sup>.

#### (3.2) Speeds

Nearly 98% of the plant ordered will operate at 3000 r.p.m. It is estimated that, by 1960, 83% of the plant in operation in the country will have a spindle speed of 3000 r.p.m., and the remaining 17%, operating at 1500 r.p.m., will become increasingly obsolete. The higher spindle speeds have an inherent advantage particularly for high-pressure work, in that they enable plant size and initial cost to be reduced, and often increase efficiency. They are also more suitable for 2-shift operation, because of their light weight, small size and low distortion under changes of temperature.

#### (3.3) Type

Single-cylinder machines have been limited to 20 MW capacity although a number of double-rotation Ljungström machines have been installed for capacities of 12.5-50 MW.

Two-cylinder machines with double-flow low-pressure exhausts are being built for 30, 60 and 100 MW sizes when operating under straight condensing cycles, although a large number of 3-cylinder machines with double-flow exhausts are being constructed for the 900 and 1500 lb/in<sup>2</sup> cycles.

Three-cylinder designs with either double- or triple-flow exhausts have been developed for the large reheat machines. Double-casing designs have been evolved for the high-pressure cylinders of extra-high-pressure turbines, the space between the inner and outer casings being in communication with an intermediate stage of the blading; this allows the inner casing to be built in sections with suitable alloys to meet the temperature conditions, and the scantlings and bolt loadings of the outer main casing can be reduced. Where single-casing designs are used for high temperatures, separate and loose high-pressure nozzle boxes are often provided so that the casing is not subjected to the initial pressure and temperature.

Table 5

## ANALYSIS OF POST-WAR PROGRAMMES IN TERMS OF CAPACITY AND PRESSURE

Set capacity	Number of sets for turbine stop-valve pressure of							Capacity of group
	300-350 lb/in <sup>2</sup>	360-415 lb/in <sup>2</sup>	600-650 lb/in <sup>2</sup>	900 lb/in <sup>2</sup>	1 235-1 350 lb/in <sup>2</sup>	1 500 lb/in <sup>2</sup>	2 350 lb/in <sup>2</sup>	
MW								MW
<i>1946-52 Programmes</i>								
22.5 and less	2	8	4	—	—	—	—	244
25-32	—	6	70	—	—	—	—	2 302.75
40-45	—	2	9	—	—	—	—	485
50-53.5	1	—	23	22	—	—	—	2 376
60-61.5	—	—	2	34	5	4	—	2 701.5
75	—	—	—	3	—	—	—	225
100	—	—	—	—	1	—	—	100
120	—	—	—	—	—	—	—	—
200	—	—	—	—	—	—	—	—
Totals .. .. .	3	16	108	59	6	4	—	—
Capacity of group, MW	92	421.25	3 888.5	3 392.5	400	240	—	8 434.25
<i>1953-59 Programmes</i>								
22.5 and less	—	6	—	—	—	—	—	105
25-32	—	—	41	—	—	—	—	1 230
40-45	—	1	1	—	—	—	—	85
50-53.5	—	—	1	3	—	—	—	206.5
60-61.5	—	—	—	87	2	5	—	5 640
75	—	—	—	—	—	—	—	—
100	—	—	—	—	—	14	—	1 400
120	—	—	—	—	—	9	—	1 080
200	—	—	—	—	—	—	1	200
Totals .. .. .	—	7	43	90	2	28	1	—
Capacity of group, MW	—	150	1 321.5	5 375	120	2 780	200	9 946.5

Special high-pressure-casing designs are being evolved for large reheat machines which incorporate two groups of blading arranged back-to-back in a common casing. One type, in which initial and reheat steam are centrally admitted to the entrance of these groups and flow outwards to exhaust branches located at each end of the casing, has already been described.

In an alternative arrangement the initial steam inlet connection only is centrally admitted to one group of blading. On discharge this steam flows back over the surface of the inner cylinder, and after passing through ports provided in its carrier ring, enters the inlet to the second group of blading. After expansion in the second group it discharges through exhaust branches provided in the outer casing, to be led to the boiler for reheating. Although the number of steam inlet and exhaust branches is halved, the work done in the two blading groups only equals that done in one group of the first design.

Designs for 3 000 r.p.m. cross-compound machines are still under review, as they would allow 200 MW sets to be built with existing designs of alternator and would materially reduce the overall expansion of the turbine casings as compared with tandem-compound turbines.

Solid couplings are now being used between the spindles of either 2- or 3-cylinder tandem-compound sets, for experience has shown that flexible-coupling lock causes dangerous wear on the thrust-block surge pads. In the 3-cylinder designs it is usual to arrange the high-pressure and intermediate-pressure cylinder blade groups back-to-back so as to relieve the load on the single thrust block employed.

Large reheat sets are provided with interceptor valves controlled by a separate speed governor arranged to close the valve with a 2% speed rise; as an additional safeguard against over-speed, emergency-trip oil-operated stop valves of the flap type are also used. The high-pressure turbine is protected by safety

valves connected to the reheater outlet header of the boiler and capable of passing the continuous maximum flow rate. As an additional safeguard the turbine manufacturer supplies oil-relay-operated steam dump valves discharging to atmosphere under emergency conditions.

#### (3.4) Blading

More efficient nozzle and blade profiles have been developed as the result of air-tunnel tests on the effects of variation in pitch/chord and aspect ratios, angles of incidence, stagger, deviation, and operation at a wide range of Mach numbers. These tests are usually followed by steam tests on combined nozzle and blade groups, or in a test turbine with a limited number of stages.

Blading is also designed for varying degrees of reaction from root to tip, to ensure a more uniform steam distribution and efficiency over the whole blade annulus of each stage. In some impulse-type machines the degree of reaction at the root of the blade may vary from zero to 17%, and from 5 to 65% at the tip of the blade. This variation in degree of reaction from stage to stage is governed mainly by the ratio of blade length to mean diameter of the stage and to a smaller extent by the pressure ratio and heat drop used in that stage.

As already mentioned, experience on large-diameter high-velocity low-pressure wheels indicates that heavy erosion can occur when operating with up to 14% wetness in the last group. Investigation into the performance of interstage drainage systems indicates that great difficulty is experienced in extracting moisture formed in the annulus between the mean and root diameters of long low-pressure blades, especially when small axial clearances are employed between the blade groups. Recent tests indicate that about 25% of the moisture carried into and generated in a pressure stage is about the maximum extraction possible.

The adoption of reheat materially relieves these extraction problems, but both British and American manufacturers believe that further work must be done on interstage drainage.

### (3.5) Condensers

The scarcity of requisite quantities of cold clean circulating water has largely precluded the use of single-pass condensers; 2-pass condensers are commonly used for river and estuary work, and the 3-pass type has been justified for some cooling-tower stations.

The adoption of increasing initial pressure and temperature, including reheat, has materially reduced the condenser surface to about 0.6 ft<sup>2</sup>/kW for a 200 MW turbine operating at 2350 lb/in<sup>2</sup> compared with 0.9 ft<sup>2</sup>/kW for a 30 MW turbine on a 600 lb/in<sup>2</sup> cycle. With the use of heavy intermittent chlorination these surfaces are ample and allow for 15% of the tubes to be plugged. There is an increase in the use of tubes expanded at both ends, suitable provision for expansion being made in the construction of the condenser shell.

To minimize the oxygen content of the condensate, ample steam lanes are provided through or around the tube nests, and the air-extraction branches are located well away from the condensate outlets and at the exit from a large vapour-condensing and air-cooling section. Rotary-type exhausters will be used on large-capacity extra-high-pressure plant to create a 20 in Hg vacuum in a period of 10–15 min under starting conditions. Air pumps of the Leblanc rotary type are being installed to avoid the expensive small-bore piping required by steam air-ejector plant.

### (3.6) Feed-Heating Plant

It has been possible to rationalize the feed-heating trains for plant included in the two latest programmes. Condenser de-aeration is considered safe for 30 MW 600 lb/in<sup>2</sup> plant, the train often incorporating a small shunt de-aerator and a large-capacity storage vessel located in the down pipe from the surge tank. This provides a reserve of de-aerated water to allow for variations in demand from the boiler in excess of normal condensate return, and also for topping-up boiler plant with de-aerated water when shut down under 2-shift operating conditions.

For plants operating at 900 lb/in<sup>2</sup> or more a high-level feed-heating de-aerator is interposed between the low- and high-pressure sections of the feed train. Heating steam or immersion heaters are provided, so that, with continuous water recirculation and air extraction, demands for boiler topping-up water can be met from the de-aerated water storage tank at all times.

The feed train for the 30 MW 600 lb/in<sup>2</sup> plant comprises two low-pressure and two high-pressure heaters with the boiler-feed suction temperature controlled at about 205° F to avoid raising the surge tank above the normal building height adopted for these sets; the final feed temperature is 224° ± 10° F.

For a 60 MW 900 lb/in<sup>2</sup> set the train comprises two low-pressure heaters, a feed-heating de-aerator and two high-pressure heaters, the final feed temperature being 375° ± 10° F.

The train for 100 MW straight condensing, or 80 and 120 MW reheat, plant operating at 1500 lb/in<sup>2</sup> comprises two low-pressure heaters, a feed-heating de-aerator and three high-pressure heaters, the final feed temperature being 410 or 435° F, respectively, ± 10° F.

The feed train of the 200 MW 2350 lb/in<sup>2</sup> plant comprises two low-pressure heaters, a feed-heating de-aerator and three high-pressure heaters with a separate drain cooler interposed between the two last heaters, the final feed temperature being 460° ± 10° F.

On large high-pressure reheat plants, split boiler feed pumps

are being used, which materially reduces the pressure to which the high-pressure heaters are subjected and thus greatly increases their reliability.

High-pressure feed heaters for use with the reheat turbines are being provided with de-superheating sections, and consideration is also being given to incorporating an integral drain-cooling section prior to cascading the condensate to the next heater.

Bled-steam evaporators for 30 MW 600 lb/in<sup>2</sup> plant are at present preferred for the extra-high-pressure frames. Multi-effect thermal-compression evaporators using live steam have been largely used for the 60 MW 900 lb/in<sup>2</sup> plant, but they can be used under bled conditions with high-pressure plant.

Demineralizing plant has been installed at some stations, and for use with high-pressure steam sets usually comprises 2-staged degassing and mixed-bed units in series. It is specified that the treated make-up water shall be free from turbidity and shall contain no dissolved carbon dioxide, shall have a total dissolved solid content not exceeding 0.5 part in 10<sup>6</sup>, including 0.05 part in 10<sup>6</sup> of silica, and shall have a pH-value of 7.0–7.2.

Since organic solids may pass through a demineralizing process, the feed-water treating plant for the 200 MW 2350 lb/in<sup>2</sup> sets will comprise a base-exchange water softener, a bled-steam evaporator and a mixed-bed demineralizing plant in series. The resultant make-up feed water is expected to contain only some 0.3 part in 10<sup>6</sup> of total dissolved solids.

The 275 MW 5000 lb/in<sup>2</sup> supercritical American set will probably use a feed-water treating plant similar to that outlined for the British 200 MW set. In addition, a second mixed-bed demineralizing plant capable of handling a substantial proportion of the total condensate may be installed. This would counteract the effects of any possible condenser-tube leakage and would serve nominally as a blowdown for the system. The demineralizing equipment is expected to provide make-up water with a total solid content of less than 0.1 part in 10<sup>6</sup>. It is also recognized that oxygen removal of the highest order is essential, and de-aeration to meet these requirements will be provided. Further elimination of oxygen and additional pH-value control may be obtained by the use of some of the standard organic additions such as morpholine or hydrazine.

The turbine and feed-heating drainage systems for large unit sets has been rationalized in an endeavour to provide simple control and self-regulation as far as possible under starting or shutting-down conditions for 2-shift operation.

All piping drains located before the turbine starting valve and subject to full boiler pressure and temperature are led to an atmospheric drains tank to discharge through master and martyr valves at the disposal point. Turbine-cylinder and pipe drains subject to pressure and superheat under load conditions, but to vacuum during starting and stopping, are led to "grouped" valves and orifice plates fitted to a large condenser flash-box. Cylinder and pipe drains subject to vacuum under all conditions are connected to a U-loop which discharges direct to the condenser.

The feed-heater shell, flash-box, and—so far as practicable—their interconnecting piping, are cleared of water by flow under gravity to a second condenser flash-box, condensate from the two flash-boxes being discharged through U-loops into a hot-Well located in the base of the condenser.

### (3.7) Auxiliaries

Turbines of 60 MW or greater output are provided with turbovisory gear, since experience has proved it essential to the operator for the safe handling of the plant.

For convenience, the instrumentation is divided into three groups. The first group comprises the recording and indicating of the speed of the set, as measured by an electrical tachometer

and of the eccentricity of the high-pressure spindle of a 2-cylinder machine, or the high- and intermediate-pressure spindles of a 3-cylinder machine. The second comprises the recording and indicating of the total expansion, as measured at the high-pressure pedestal, and the differential expansion between the motor and casing of the high- and intermediate-pressure cylinders, the recording of steam and metal temperatures of the high- and intermediate-pressure cylinders, and the recording of the generator output. The third group, which is optional, comprises the recording and indicating of vibration of the pedestals and of the main thrust-block clearances. It is usual to locate the recording and indicating instruments in the central plant control room, but some of the dials may be duplicated for the guidance of the turbine driver.

Large sets are provided with a vacuum-operated bellows relay which causes the main governing valves to close as the vacuum in the condenser falls to 25 in Hg, and automatically re-opens if the fault is of short duration. A pressure-operated switch is also provided, which energizes a solenoid attached to the emergency trip gear; when the vacuum falls to about 15 in Hg the emergency stop valve is tripped and prevents the machine from going over to atmospheric working.

A number of unit sets have pressure-operated load pay-off and trip gear which safeguards the plant in the event of coal sticking in the boiler chutes or accidental mill stoppage, etc. A pressure-operated relay causes partial closure of the main governing valves if the steam pressure gradually falls to a predetermined limit and allows them to re-open if the fault is of very short duration. The pressure-operated switch energizes a second solenoid associated with the emergency trip gear, which comes into action when the boiler pressure falls to a dangerous extent and trips the turbine emergency valve.

The testing of emergency governor trip gear and the closure of intercept valve gear under load conditions is called for on all larger machines.

Motor-driven barring gear is fitted to all machines for use when warming through and taking off load, but lately the speed of rotation has been increased.

The use of jacking pumps is now mainly confined to the larger frames.

#### (4) BOILERS

##### (4.1) Size of Units

A total of 579 boilers with a combined evaporative capacity of 185 673 500 lb/h were included in the two programmes, and 372 boilers with a total capacity of approximately 100 000 000 lb/h have already been commissioned.

The size of the boiler unit installed has increased very rapidly in the post-war years, as the following analysis shows:

263 boilers of from 80 000 to 265 000 lb/h capacity provide 50 348 500 lb/h, or 27.1% of the total capacity, and are mainly installed at extensions to existing generating stations.

209 boilers of from 300 000 to 450 000 lb/h capacity provide 69 580 000 lb/h, or 37.5% of the total capacity, and are mainly installed at the smaller new stations or new sections of stations at existing sites.

107 boilers of from 515 000 to 860 000 lb/h capacity, together with one 1 400 000 lb/h unit capacity, provide 65 745 000 lb/h, or 35.4% of the total capacity, and are to be installed at new large stations.

In subsequent years mainly three sizes of reheat boiler plant will be installed, and consideration is being given to introducing the forced-flow once-through type, especially at reconstructed stations. Some 24 turbines ranging in output from 100 to 200 MW are associated with 24 boilers aggregating 20 460 000 lb/h evaporative capacity. These 24 combined plants provide 15.1% of the total power requirements for 11% of the boiler evaporative capacity ordered in the second post-war programme.

Classification of boilers by steam conditions at the turbine stop valve indicates that they fall into four broad groups, namely

(a) 49 boilers for 300–415 lb/in<sup>2</sup> cycles provide 7 562 500 lb/h, or 4.1% of the total.

(b) 255 boilers for 600–650 lb/in<sup>2</sup> cycles provide 60 186 000 lb/h, or 32.4% of the total.

(c) 228 boilers for 900 lb/in<sup>2</sup> cycles provide 87 075 000 lb/h, or 46.8% of the total.

(d) 47 boilers for 1300, 1500 and 2350 lb/in<sup>2</sup> cycles provide 30 850 000 lb/h, or 16.7% of the total.

The classification of boilers by capacity at the continuous maximum rating and by steam conditions at the turbine stop valve is shown in Table 6.

##### (4.2) Rating

Prior to interconnected operation on the national Grid system it had been customary for municipalities and power companies to have boiler and turbine plant designed to have its most economic point at 80% of a short-period overload rating. It was contended that the overload rating was called upon only at times of peak load or in emergency, and that the normal economic rating was more important because it determined the general efficiency of the plant, since it would operate for the major portion of its life at this rating. Experience gained with such plant under interconnected station conditions showed that the boiler availability was low when attempts were made to run it for long periods at the overload rating, and that the running of such plant at the normal economic rating involved the long-term use of inferior plant at other stations, and substantial commercial losses were incurred. Since plant may be block-loaded under interconnected operation, it was decided that future boiler and turbine plant should be designed for continuous maximum rating and be capable of continuous service for six months with only on-load cleaning of the boiler heating surfaces. Experience gained in operation indicated that stoker-fired plants might meet this condition and that pulverized-fuel-fired plants might give continuous service for up to 12 months.

It was agreed with the manufacturers that efficiency tests should be taken with the boiler in a clean condition, and that check tests should be made at the end of the guarantee period. An efficiency tolerance of 3½% was allowed for stoker-fired boilers and of 2½% for pulverized-fuel-fired boilers; most of the check tests have shown little or no decrease in boiler efficiency. By mutual agreement these rigorous tests are based on the boilers being supplied with the fuels for which they were designed, the continuous maximum rating of the boiler not being exceeded at any time during the test.

The achievement of substantially the same availability of boilers and turbines has justified the adoption of the unit system for even very-large-capacity plant designed for extra-high-pressure and temperature conditions. This has been accomplished by close co-operation between manufacturers and the industry; in 1939 discussions were instituted to improve the then low availability of boilers, and these led to the appointment of a Boiler Availability Committee comprising representatives of the manufacturers, research associations and the industry.

##### (4.3) Relationship between Boiler and Turbine Capacities

The ratings of boiler units relative to turbine capacity for most of the plant in the 1946–52 programme was governed by the size of boiler available. To accelerate construction, some stations were built in sections comprising four boilers and two turbines; further sections of the stations were then built to the same drawings. The boilers were chosen so that each three would provide the continuous maximum rating of the two turbines, together with any station auxiliary steam required. This was

Table 6

ANALYSIS OF POST-WAR PROGRAMMES IN TERMS OF CAPACITY AT MAXIMUM CONTINUOUS RATING AND STEAM CONDITIONS

Evaporative capacity	Number of boilers operating at turbine stop-valve pressures and temperatures of										Number in group	Capacity of group
	250 lb/in <sup>2</sup> 775° F	300-350 lb/in <sup>2</sup> 750-800° F	360-415 lb/in <sup>2</sup> 800-825° F	600-650 lb/in <sup>2</sup> 800-900° F	900 lb/in <sup>2</sup> 900-925° F	1235-1350 lb/in <sup>2</sup> 825-950° F	1500 lb/in <sup>2</sup> 1050° F	1500 lb/in <sup>2</sup> 975/950° F*	1500 lb/in <sup>2</sup> 1000/ 1000° F*	2350 lb/in <sup>2</sup> 1050/ 1000° F*		
lb/h × 10 <sup>3</sup>												lb/h × 10 <sup>3</sup>
<i>1946-52 Programmes</i>												
<80	2	—	—	—	—	—	—	—	—	—	2	160
100-150	—	4	8	13	—	—	—	—	—	—	25	3295
160-190	—	1	17	78	—	—	—	—	—	—	96	17183.5
200-230	—	—	5	19	2	—	—	—	—	—	26	5650
240-265	—	2	—	17	21	4	—	—	—	—	44	10870
300-330	—	—	—	48	40	—	—	—	—	—	88	27260
350-375	—	—	—	18	42	—	—	—	—	—	60	21620
400-450	—	—	—	4	5	6	—	—	—	—	15	6195
500-550	—	—	—	—	7	2	4	—	—	—	13	6935
600-900	—	—	—	—	—	—	—	—	—	—	—	—
>900	—	—	—	—	—	—	—	—	—	—	—	—
Number in group	2	7	30	197	117	12	4	—	—	—	369	—
Capacity of group, lb/h × 10 <sup>3</sup>	160	1142.5	4930	47376	38825	4675	2060	—	—	—	—	99168.5
<i>1953-59 Programmes</i>												
<80	—	—	—	—	—	—	—	—	—	—	—	—
100-150	—	—	8	9	—	—	—	—	—	—	17	2300
160-190	—	—	1	25	—	—	—	—	—	—	26	4680
200-230	—	—	1	—	6	—	—	—	—	—	7	1400
240-265	—	—	—	5	15	—	—	—	—	—	20	4810
300-330	—	—	—	18	17	—	—	—	—	—	35	10630
350-375	—	—	—	1	10	—	—	—	—	—	11	3950
400-450	—	—	—	—	—	—	—	—	—	—	—	—
500-550	—	—	—	—	63	2	5	—	—	—	70	38275
600-900	—	—	—	—	—	—	10	4	9	—	23	19060
>900	—	—	—	—	—	—	—	—	—	1	1	1400
Number in group	—	—	10	58	111	2	15	4	9	1	210	—
Capacity of group, lb/h × 10 <sup>3</sup>	—	—	1330	12810	48250	1080	10875	3020	7740	1400	—	86505

\* Reheat boilers.

prior to the Authority being able to make arrangements with the National Coal Board for the allocation of more regular supplies of suitable fuels to the generating stations. With allocated fuels it was possible to curtail the number of boilers installed, to increase the capacity of the later turbines, or even to install an additional turbine.

At a later date the boiler rating was chosen so that with four boilers in commission they were steaming at about 90% load, or that with one boiler out of commission some 82% of the turbine capacity was still available.

For unit systems the boiler rating is about 7½% more than the rated continuous maximum demand at the turbine stop valve, to provide station auxiliary steam and allow a margin for emergency fouling of the turbine blading or boiler heating surfaces.

#### (4.4) Water Walls and Boiler Convective Heating Surfaces

As previously mentioned, some of the plant in the first 7-year programme, especially that for 600 and 900 lb/in<sup>2</sup> cycles, had to be built to modified designs of existing straight-tube and header, or multi-drum bent-tube, boilers. Since steel was scarce, material reductions were made in the amount of boiler convection heating surface used in two ways. First, by increasing the amount of high-heat-transfer radiant absorbing surface in the combustion

chamber its gas discharge temperature was materially reduced; this permitted substantial reductions in the convective boiler surface between the combustion-chamber exit and the superheater inlet. Secondly, the use of higher operating pressure and temperatures considerably increased the amount of heat absorbed by the superheater and reduced the amount of heat required for water preheating and evaporation. Gas temperatures at exit from the superheater were now sufficiently lowered to discharge direct to the economizer, thereby eliminating the secondary bank of boiler convective heating surface. This enabled single- or 2-drum bent-tube designs to be developed with only a widely spaced tube screen between the combustion chamber exit and the superheater inlet.

Water-wall construction changed from wide-pitched tubes with cast-iron-faced blocks to finned or tangent tubes which present a continuous metal surface in the combustion chamber. With the evolution of tall water-cooled combustion chambers for stoker-fired, and particularly for pulverized-fuel-fired, boilers, circulation in the water walls presented no difficulty, since liberal downcomer pipes were arranged in cooled zones. The fitting of cyclones in the drum to separate steam bubbles from the water entering the drum, together with increases of operating pressure, permits the use of smaller drums and the elimination of diaphragm drums, and supplies steam of high purity to the superheater.

#### (4.5) Combustion Chambers

The rapid increase in boiler capacity for ever-increasing pressures and temperatures, including the incorporation of a reheater section in the boiler gas-passes, created new problems in combustion-chamber design, particularly when associated with the use of low-grade fuels of high ash content and low deformation temperature.

The average gas temperature at the exit of the combustion chamber is mainly dependent upon the amount of water-wall radiant absorbing surface provided for a given total heat release in the combustion chamber. The volumetric heat release has little influence on this temperature, but merely indicates the time available for combustion.

If the configurations of combustion chambers are the same, the heat release per square foot of wall surface increases as the cube root of the boiler evaporative capacity for constant volumetric heat release; for boiler capacities of 930 000 and 1 000 000 lb/h this would be 1.67. In these circumstances the heat release per square foot of wall surface would be 100 000 B.Th.U./ft<sup>2</sup>/h for the large chamber compared with, say, a design figure of 60 000 B.Th.U./ft<sup>2</sup>/h in the smaller chamber. This would result in gas being discharged from the large chamber some 300°F hotter than that from the smaller chamber. It would therefore be necessary to alter the proportions of the large chamber and to provide a division wall to reduce the temperature to safe limits—which emphasizes the statement made that volumetric heat release alone has little influence on gas temperature.

However, if at the continuous maximum rating the combustion-chamber-gas exit temperature approximates to the ash softening point determined under semi-reducing conditions, little difficulty is experienced with slagging on well-spaced chamber outlet tubes or well-spaced platen-type superheaters.

As the capacity, pressure and temperature increase, and particularly if reheat is required, the fraction of the total heat which can be absorbed in the water walls of the combustion chamber becomes progressively smaller. With large reheat boilers it is sometimes necessary to provide some steam-cooled surface in the combustion chamber in the form of radiant superheater surface if the required combustion-chamber exit temperature is to be obtained; to attain very low exit temperatures would therefore require the use of considerable radiant superheater and reheater surfaces to line the chamber walls. This leads to very large wall areas unless the radiant superheater and reheater surface is introduced into the top of the combustion chamber in the form of pendant platens.

In Britain the limit of capacity with a single combustion chamber consuming low-grade coal of high ash content and low deformation temperature is at present about 120 MW. The use of a divided combustion chamber, or two separate combustion chambers (which materially eases boiler construction), enables unit reheat boilers to be designed for 200 MW or, in the future, possibly 240 MW.

Two other factors affect combustion chamber performance. First, recirculation of the cool and inert combustion gas from the economizer outlet may be used (a) to lower the gas temperature entering the superheater zone by some 200°F, with a minor change only in steam temperature, by mixing some 20–25% of cool gas with the hot gas traversing the open-pass section of the chamber, and (b) to achieve some degree of steam temperature control by injecting 8–10% of cool gas into the combustion chamber ahead of the burner zone. Since recirculation tends to suppress the formation of bonded deposits on heating surfaces, provision is being made for the future application of (a) and (b) on some boilers. Secondly, if a very high heat release per square foot of chamber horizontal cross-sectional area is used, it limits

the space available at the burner levels, especially in corner-fired boilers, and leads to high local heat release and also high gas velocities through the chamber.

If very large areas of radiant superheater and reheater surface are incorporated in the combustion-chamber walls, so as almost to eliminate the expensive convective superheating and reheating surface, it becomes necessary to provide extended boiler convective heating surface in the downcomer chamber of the plant.

#### (4.6) Firing

Of the 579 boilers ordered under the two programmes, 355 are pulverized-fuel fired, 220 are stoker fired and four are oil fired; the respective total evaporative capacities of these groups are 142 745 000, 41 428 500 and 1 500 000 lb/h, representing 76.8, 22.4 and 0.8% of the total capacity. As will be seen from Table 7, with the exception of three retort-stoker and four oil-fired plants, all boilers having an evaporative capacity of 300 000 lb/h or more are pulverized-fuel fired. Most of the stoker-fired boilers, and some of the smaller pulverized-fuel-fired boilers, were installed as extensions to existing generating stations.

The progress of pulverized-fuel firing can be gauged by the fact that, whereas in 1945 some 18 000 000 tons of fuel were consumed in stoker-fired boilers and only about 4 500 000 tons in pulverized-fuel-fired boilers, in the year ending 31st December, 1954, the figures were 20 270 000 and 18 110 000 tons respectively—an increase of about 300% in pulverized-fuel consumption.

Spreader-stoker firing was adopted at one new station, where sixteen 240 000 lb/h boilers were installed to provide steam for six 60 MW turbo-generators. A further eight 172 000 lb/h boilers were installed at an extension to an existing generating station to provide steam for two 51.5 MW turbo-generators. In addition, two 200 000 lb/h boilers were installed as extensions to a third existing generating station. All the above plant has proved satisfactory in service and demonstrated its eminent suitability for the export market.

A 540 000 lb/h boiler fitted with three cyclone chambers and capable of operating under either balanced-draught or pressurized conditions is being installed for experimental purposes. Arrangements have been made to obtain coal supplies from a wide number of National Coal Board Divisions so that the combustion apparatus can be thoroughly tested with a wide variety of fuels.

All the pulverized-fuel-fired plants are provided with mills operating on the unit system. At a number of large riverside stations the pulverized-fuel-fired boiler plant is being converted for oil burning in order to ease the coal situation.

#### (4.7) Superheaters, Reheaters and Economizers

The divided or separate combustion chambers and the rear chamber of large boilers are usually interconnected by means of nozzle-shaped horizontal flues. In some designs the rear chamber is divided into three compartments by vertical walls, the out-flanking compartments housing the primary section of the superheater and sections of the economizer, and the centre compartment the primary reheater and the remaining sections of economizer; dampers at the exit of each compartment permit the weight of gas flowing over the reheater or superheater sections to be varied, and so permit some degree of temperature control. The pendant-type secondary sections of both superheater and reheater are housed in the nozzle-shaped flues.

In other boiler designs the common rear chamber houses the economizer sections and primary section of the superheater only, and the flues connecting the two separate combustion chambers house the pendant secondary section of the superheater in one and the whole of the pendant reheater in the other.

Table 7

## ANALYSIS OF BOILER PROGRAMMES IN TERMS OF CAPACITY AT CONTINUOUS MAXIMUM RATING

Rated capacity	Number of boilers			Capacity			Fraction of programme	
	Pulverized-fuel fired	Stoker fired	Total	Pulverized-fuel fired	Stoker fired	Total	Pulverized-fuel fired	Group
lb/h $\times 10^3$				lb/h $\times 10^3$	lb/h $\times 10^3$	lb/h $\times 10^3$	%	%
<i>1946-52 Programmes</i>								
<80	—	2	2	—	160	160	—	0.2
100-150	1	24	25	135	3 160	3 295	0.4	3.3
160-190	11	85	96	1 880	15 303.5	17 183.5	11.0	17.4
200-230	2	24	26	400	5 250	5 650	7.0	5.7
240-265	18	26	44	4 450	6 420	10 870	41.0	11.0
300-330	85	3	88	26 315	945	27 260	95.6	27.5
350-375	56	—	60*	20 120	—	21 620†	93.0	21.8
400-450	15	—	15	6 195	—	6 195	100.0	6.2
500-550	13	—	13	6 935	—	6 935	100.0	6.9
750-900	—	—	—	—	—	—	—	—
>900	—	—	—	—	—	—	—	—
Totals .. ..	201	164	369*	66 430	21 238.5	99 168.5†	67.1	100.0
<i>1953-59 Programmes</i>								
<80	—	—	—	—	—	—	—	—
100-150	4	13	17	600	1 700	2 300	26.0	2.7
160-190	—	26	26	—	4 680	4 680	—	5.4
200-230	—	7	7	—	1 400	1 400	—	1.6
240-265	10	10	20	2 410	2 400	4 810	50.0	5.6
300-330	35	—	35	10 630	—	10 630	100.0	12.3
350-375	11	—	11	3 950	—	3 950	100.0	4.6
400-450	—	—	—	—	—	—	—	—
500-550	70	—	70	38 275	—	38 275	100.0	44.2
750-900	23	—	23	19 060	—	19 060	100.0	22.0
>900	1	—	1	1 400	—	1 400	100.0	1.6
Totals .. ..	154	56	210	76 315	10 190	86 505	88.2	100.0
Cumulative totals	355	220	579*	142 745	41 428.5	185 673.5†	76.8	100.0

\* Including four oil-fired boilers.

† Including 1 500 000 lb/h due to oil-fired boilers.

With either natural or assisted circulation through the water walls of both combustion chambers, the boiler can be started up and the pulverized-fuel burners lit in the combustion chamber devoted to superheat only, and the turbine can be run up and synchronized as a straight condensing machine. The pulverized-fuel burners in the reheating combustion chamber are then lit and the stop-valve and reheat steam temperatures adjusted to suit loading conditions on the by turbine further adjustment of the burners in each of the combustion chambers. The first 200 MW boiler will be of this type, and will be provided with assisted circulation through the water walls.

The secondary superheaters of these large boilers are of the platen type, arranged on 12-14 in centres, and they absorb a large fraction of radiant heat; succeeding sections are arranged on 6-8 in centres, and when the gas temperatures have dropped to about 1 500° F the tube pitch is reduced to 3½-4 in. In general, the economizer surface is of the plain tube type with square pitching, again of 3½-4 in.

#### (4.8) Grit-Arresting Plant

At large generating stations it is usual to provide combined mechanical and electrostatic grit-arresting plant arranged in series. The mechanical section is sometimes combined with the electrostatic section in a common casing and is designed to give a fairly uniform gas velocity through the electrical section. Experiments made in operation indicate that less than 1% of the dust burden presented to the grit-arresters escapes to the stack, and that under electrical fault conditions a major proportion of the grit is still extracted by the mechanical section. Considerable

research is in progress, including that on a high-gas-velocity design developed to have high extraction efficiencies with dust sizes from 10 down to 1 micron and gas speeds of 20-50 ft/sec through two sections in series. This design incorporates a pre-ionizing zone in each of the two collecting zones.

#### (4.9) Instrumentation

A central control room for each pair of sets and boilers installed is being adopted at some of the new large generating stations. It is recommended that all windows overlooking both turbine and boiler rooms should be fitted with double-polarized glass, to avoid both glare and parallax.

The control desks are preferably located in the centre of the room and should face the instrument panels which line the side walls. A rational grouping of the instruments and controls between various services associated with each set can be obtained by mounting the instruments on four distinct panels: the first panel and control-desk section would be associated with the turbo-generator, exciter and hydrogen plant, the second with the water and steam loops (extraction pumps, feed train, boiler feed pumps, boiler steam range and turbine), the third with the combustion loop (bunker outlet valves, mills, exhausters, fans, combustion chamber, boiler passes, automatic combustion control gear, grit-arresters and stack) and the fourth panel with the electrical auxiliary services of the set and boiler. A soot blowing control cabinet may be located adjacent to the auxiliary panel. Each of the four main panels may have 36 annunciator alarm points at eye level, so that the control engineers have nearly 144 metering and alarm points under ordered observation.

Television equipment for checking combustion conditions may become a necessity, especially if pressurized combustion is more widely adopted.

All turbovisory instruments may be mounted on panels at right angles to the four main panels and near the access doors leading to the engine room. Periscopes may also be provided to enable the control engineers to observe the movement of boiler assistants at various gallery levels.

Since metering stations are now located adjacent to each point of measurement, the equipment should be housed in dust-tight cubicles, whose upper parts are provided with indicating instruments which can be read through bulkhead-type water- and dust-tight windows. The provision of indicating instruments at each metering point permits both the boiler assistants and the control engineer frequently to check the accuracy of the control-room instruments. Each central plant control room should be in communication with the main electrical control room of the station; in an emergency, action is taken from the plant control room after the main control has been advised of the development of such a condition.

### (5) SPECIAL DESIGNS OF BOILER

Although no further once-through boilers of the Benson or Sulzer type have been installed in this country since 1939, steady progress has been made in their development on the Continent. At the end of 1954 one manufacturer had more than 70 Benson-type boilers installed or on order, including one 660 000 lb/h unit for supercritical pressures and several 1 000 000 lb/h units for subcritical pressures. Another manufacturer is building cyclone-fired once-through Benson boilers, each of more than 500 000 lb/h capacity, for subcritical pressures, and is also developing designs for large supercritical-pressure plant.

A number of Sulzer once-through boilers of 500 000 lb/h or higher capacities have been installed at large generating stations and incorporate two separate circulating systems in one combustion chamber. Designs for supercritical pressures are in progress. In addition, many boilers of this type have been installed in industry for combined process-steam and electrical-power generation.

A few forced-water-circulation boilers of the La Mont type, provided with chain-grate stokers, have been installed in reconstructed generating stations in Britain, and also in industry for combined process-steam and power generation; they have also been developed for space-heating purposes.

On the Continent a number of special Velox boilers and power plants have been installed in the steel industry for providing the combined compressed-air and electricity requirements of the works. Other Velox plants have been supplied to various parts of the world to provide standby plant in hydro-electric stations. An interesting space-heating installation is in progress in London.

Two further forced-steam-circulation boilers of the Loeffler type have been installed, and operating experience proves that the internal surfaces of the steam circulating system are in excellent condition after up to 17 years' service, and that extra high pressures may be adopted with safety.

Similarly, the experience gained with special alloy steels and welding are proving of great value when contemplating the use of pressures up to 2350 lb/in<sup>2</sup>.

## (6) MATERIALS AND CONSTRUCTION

### (6.1) Materials

To meet the rapid growth of power demands with increased thermal efficiency, large-capacity sets operating at extra high steam pressures and temperatures must be used, and this necessi-

tates alloy steels with high creep stresses at elevated temperatures so that turbine cylinders, valve gear and other components may have reasonable cross-sectional areas to avoid lack of flexibility or high internal thermal stresses under changing conditions.

Since the tolerances permissible in turbine construction are very small, reliable creep data are essential when strains of 1 mil/in may have to apply to many components with a life of 150 000 hours under continuous base-load conditions. A strain of 3 mil/in may be used for superheater tubing and pipework, and in this case hot rupture tests form a valuable guide until full creep data are available. In superheater tubing the possible corrosion due to deposits forming on the outer surfaces subject to scrubbing by combustion gases has also to be taken into account. For both turbine and boiler components, resistance to oxidation of surfaces in contact with the steam must be considered.

The steel-makers, boiler and turbine manufacturers, metallurgical research associations, the National Physical Laboratory and the Authority as the user have integrated their combined test resources to undertake the planned examination of numerous alloy steels. At present the general opinion is that turbine components may be constructed from special ferritic steels for stop-valve temperatures up to 1050° F; for higher temperatures the limited use of austenitic materials for certain components is contemplated.

Chromium-molybdenum, chromium-molybdenum-vanadium, and chromium-molybdenum-vanadium-tungsten alloys are being used for cylinder and control-valve castings; chromium-molybdenum and chromium-molybdenum-vanadium spindle forgings are available. Experience indicates that relatively low percentages of an alloying element materially improve the creep properties, and 1-2½% chromium, ½-1% molybdenum, ¼-½% vanadium and 0-8% tungsten are generally used for turbine components. Austenitic steels containing about 18% chromium, 11-13% nickel and less than 1.5% manganese, preferably niobium balanced, are available as small forgings or medium-size castings.

The manufacture of turbine-cylinder castings for high-temperature operation has necessitated the development of more scientific production controls in the foundry, the pickling of castings by safe processes prior to non-destructive methods of examination by means of X-ray, γ-ray and ultrasonic apparatus, together with special welding techniques for rectification before delivery to the turbine manufacturer's works for finish machining. The manufacture of satisfactory forgings has likewise led to the development of more scientific control of heat treatment, to ensure that uniform thermal expansion of alloy-steel shafts, discs or monoblock rotor forgings is obtained throughout so that they shall not distort with the temperature changes which inevitably occur in operation.

### (6.2) High-Pressure and High-Temperature Steam Pipework

Most of the pipework associated with the 1946-52 programme mainly comprised 600 and 900 lb/in<sup>2</sup> plant which was fabricated with seal-weld flange construction. This procedure was adopted because of the scarcity of highly skilled welding operators then prevalent, and because manufacturers had not developed suitable valve designs for butt-welded pipelines. Operating experience showed that the use of loose flanges in certain locations led to serious trouble, and many were later replaced by hub-type flanges butt welded to the pipe.

For operating temperatures up to 950° F, hot- or cold-drawn pipes made from steel containing 1% chromium and ½% molybdenum steel were used. Later the extensive use of butt welding became possible as a result of investigations into the preheating, welding and subsequent stress relieving by heat treatment. This work also included research into the correct

form of pipe ends for butt welding and the composition of the electrodes used.

The pipework associated with plant operating at or above 1 500 lb/in<sup>2</sup> and 1 000–1 050° F is made mainly from three different alloy steels, two ferritic and one austenitic. The first ferritic steel contains 2½% chromium and 1% molybdenum, and the second contains 1% chromium, 0.5% molybdenum and ¼% vanadium; the austenitic steel contains 18% chromium and 11–13% nickel, niobium balanced. In addition to extensive creep and rupture tests on these materials, there are in progress large-scale investigations on the behaviour of the weld metal and the affected high-temperature zones between the weld and the pipe material.

Steel makers and manufacturers have carried out large-scale development and research on transition pieces having one end of austenitic and the other of ferritic steel for interposition between an austenitic pipeline and the ferritic components of the boiler or turbine plant. This includes the making of forged welds by a drawing process, using hydraulic presses and suitable dies; to prevent carbon migration across the forged weld the components are nickel plated before fabrication.

Flange joints are still required in the pipeline connecting the governing control-valve chest with the top half of the cylinder casing, so that the turbine casing may be opened for inspection.

Steel makers, boiler and turbine manufacturers, pipe fabricators and the Authority have collaborated in full-scale field tests on the accurate measurement of the steam temperatures across the bore of the pipe, and the temperature gradients through the pipe wall and through the flange and its bolting, when starting up a plant, first from cold after a weekend shut-down and secondly under the short-shut-down conditions which occur with 2-shift operation. This work has definitely confirmed that steep temperature gradients may occur under certain conditions, and the data obtained will form a valuable guide towards the evolution of more suitable methods of plant operation.

Field work is also being carried out on the thermal cycling of transition pieces, which are made up into a circuit shunting the main pipework of the set.

By the same joint collaboration the behaviour of the welded joints under controlled thermal cycling conditions is being investigated by installing two electrically heated furnaces in a generating station where steam at 1 900 lb/in<sup>2</sup> is available. Six steel bottles, each incorporating a test weld, can be tested simultaneously in each furnace. The preliminary rapid heating will be carried out by condensing the high-pressure steam until the weld metal attains the saturation temperature of the steam corresponding to its pressure. Further increases of temperature will be achieved by the circulation of hot air over the bottles, while they are still under pressure, until the temperature equals or exceeds that under service conditions. The two furnaces are fitted with complete automatic control apparatus so that they require the minimum of attention while carrying out accelerated thermal cycling of the test welds. By this means advanced information will be obtained on the behaviour in service of various welding techniques and electrode metals, for the test should simulate in the space of 1–2 years a life of, say, 10 years; after this test the welds will be sectioned and examined. These tests are not designed to impose bending stresses on the welded joint, but consideration is being given to modification of the apparatus to allow this to be done at a future date.

## (7) ELECTRICAL EQUIPMENT

### (7.1) Generators

The winding of generator stators for 33 kV or higher voltages, in order to eliminate step-up transformers, was abandoned during

the war for two reasons: first, they had been developed primarily for use with municipal or power-company systems when the load on their local distribution systems justified reinforcement by additional 33 kV transmission networks, but the war-time reinforcement of the Grid and the demand for increased plant capacities and higher transmission voltages again necessitated the use of generator transformers; secondly, generators wound for lower voltages could be manufactured in three months less time—a most important factor during the serious plant shortages of the war and post-war periods.

When the 3 000 r.p.m. 60 MW tandem-compound turbines were being developed it was realized that hydrogen cooling would materially reduce the active material in the stator and rotor for the same temperature rise in the windings, because of the lower density and superior cooling properties of hydrogen; again its importance was emphasized by the scarcity of materials.

Since the manufacturers had to develop designs and retool their works for production when material and man-power were scarce, progress was slow and the 1946–52 programme comprised 158 air-cooled machines (total capacity of 6 194.25 MW) and 38 hydrogen-cooled machines (total capacity 2 240 MW). Conversely, the 1953–59 programme comprised 57 air-cooled machines (total capacity 1 886.5 MW) and 114 hydrogen-cooled machines (total capacity 8 080 MW), the latter thus providing 81½% of the capacity ordered.

Operating experience in the stations indicates that with the provision of suitable auxiliaries the hydrogen-cooled machines are as easy to operate as the air-cooled ones. It has also been found that by simple manipulation of the control valves associated with the dirty- and clean-water heat exchangers of the system when off-loading the machine, the re-cooled hydrogen temperature may be raised to 80–85° F prior to shut-down. Moreover, this temperature will not fall by more than 4–5° F even in a 36–56 hour shut-down period, because the heat losses by radiation, conduction and convection on a normal generator casing are small.

The introduction of semi-direct cooling of the rotor winding permitted further reductions in the active material required by reducing the length of both the rotor body and stator core. Recent developments in pressurized direct gas cooling of the rotor winding and pressurized gas or liquid cooling of the stator bars now enables a 200 MW generator to be built with a rotor only some 10% greater in diameter and about the same length as an early 60 MW generator with low-pressure hydrogen cooling.

### (7.2) Large Transformers

With the exception of a few units installed before 1928, all Grid transmission and generator transformers stepping up to 132 and 275 kV are 3-phase core type, of either 3- or 5-limb construction, depending on size and make. All Grid transmission and modern generator transformers are fitted with on-load tap-changing equipment.

Transformers connected to the 132 and 275 kV systems are effectively earthed, i.e. directly earthed at each transformer neutral, and are provided with windings with graded insulation. With double-wound units the on-load tap-changing equipment is connected to tapplings located at or near to the neutral end of the winding. In transmission transformers for interconnection between the 132 and 275 kV systems an auto-connection is used and the tap-changing equipment in the majority of cases is connected to tapplings at the 132 kV point of the windings. With auto-transformers installed at substations where unidirectional power flow is anticipated, neutral-end tap-changing equipment covering a range of ±10% is employed, off-circuit biasing tapping for ±5% being provided for selection of a range appropriate to the installed condition. Transformer windings for voltages below

32kV are fully insulated, the insulation test levels being as shown in Table 8.

**Table 8**  
INSULATION TEST LEVELS WITH 1/50 MICROSEC WAVE

Nominal voltage	Impulse test level
kV	kV (peak)
275	1 050
132	550
66	350
33	200
11	100

Transmission transformers rated above 10 MVA are specified with a mixed form of cooling, a separate cooling bank providing or conditions up to 50% of the rating under natural-flow cooling (type ON) with forced cooling employing fans and pumps (type OFB) providing the full rating. For generator transformers, forced-oil water (type OWF) or forced-oil air-blast (type OFB) cooling is specified, depending on the requirements of the local conditions, with standby coolers. Transformers rated at or below 10 MVA are specified with natural-flow cooling (type OM).

Apart from a few isolated cases, all transmission transformers are fitted with bushing-type terminations of anti-fog configuration. Generator transformers stepping up to 132kV are fitted with either cable boxes or bushings, depending on local conditions, but all units stepping up to 275kV are fitted with bushings. A conventional form of bushing is used for voltages up to and including 132kV, but for 275kV the type of bushing specified involves an under-oil end of a re-entrant form.

Standard winding connections are shown in Table 9.

**Table 9**

STANDARD WINDING CONNECTIONS

Voltage ratio	H.V. winding	L.V. winding
kV		
275/132	Star auto	Star auto
275/66	Star	Star
275/11*	Star	Delta
132/66	Star	Star or delta†
132/33	Star	Delta
132/11*	Star	Star or delta†

\* For generator transformers the voltage depends on the generator.

† For transmission transformers the connection depends on local conditions; standard phasing requires star/star connections in each case. With transmission transformers where delta secondary windings are involved, links for selection of alternative delta connections are specified to assist substation arrangements.

Table 10 shows the generator transformers installed in 1946–52, which are mainly associated with small new stations or reconstructed stations, and Table 11 those scheduled for installation in 1953–59, which mainly comprise the new standards.

Tables 12 and 13 show a similar grouping in respect of Grid transmission transformers.

(7.3) Switchgear

Open-type 132 and 275kV switching stations are now the customary method of connecting generating stations to the transmission network. They occupy considerable areas, and it is sometimes necessary to place the switchyard some distance from the generating station to obtain a suitable site, and then the control and intertripping arrangements present special problems. Control by light current at 50 volts over telephone-type pilots is now becoming general; it is necessary to divide the

**Table 10**

CLASSIFICATION OF GENERATOR TRANSFORMERS IN THE 1946–52 PROGRAMME

Generator voltage	Switching voltage	Number at various ratings						Group totals	
		88·75–73 MVA	70 MVA	66·7–40·5 MVA	37·5–36 MVA	35·3–34·3 MVA	33–15 MVA	Number	Capacity
kV	kV								MVA
33	132	6	3	13	1	—	—	24	1 387·75
15									
14									
12·5									
11	132	—	1	18	—	—	4	23	1 244
11·6									
14	66	4	2	6	3	—	—	15	925·4
11									
10·5									
11·8	66	1	—	—	—	—	—	1	75
13·2	33	1	—	6	7	8	6	28	1 065·85
12·25									
11·5									
11·4									
11									
10·5									
6·6									
11·8	33	—	—	1	2	—	—	3	132·5
Group totals	Number ..	12	6	44	13	8	10	93	
	Capacity, MVA	919·75	420	2 515·4	485·4	279·1	255·75		4 874·5

Table 11  
CLASSIFICATION OF GENERATOR TRANSFORMERS IN THE 1953-59 PROGRAMME

Generator voltage	Switching voltage	Number at various ratings								Group totals	
		210 MVA	144 MVA	120 MVA	88.75 and 73 MVA	72 and 70 MVA	66-40.5 MVA	37.5 and 36 MVA	32-16 MVA	Number	Capacity
kV 16.5	kV 275	1	—	—	—	—	—	—	—	1	MVA 210
13.8	275	—	2	5	—	—	—	—	—	7	888
33 15 12.5 11	132	—	—	—	8	3	3	2	—	16	1057
13.8	132	—	4	7	—	1	—	—	—	12	1388
11.8	132	—	—	2	4	82	7	20	—	115	7573
13.8	66	—	2	—	—	—	—	—	—	2	288
15 11	66	—	—	—	2	1	1	—	—	4	296
11.8	66	—	—	—	—	3	3	6	—	12	606
11.8	33	—	—	—	1	6	—	16	3	26	1177
13.2 11.6 11 6.6	33	—	—	—	1	2	3	2	4	12	512.75
11.6 11.8	22 21.5	—	—	—	—	2	—	2	1	3 2	250
Group totals	Number ..	1	8	14	16	100	17	48	8	212	
	Capacity, MVA	210	1152	1680	1233.5	7182	1002.5	1745.5	202.25		14345.75

Table 12  
CLASSIFICATION OF GRID TRANSFORMERS IN THE 1946-52 PROGRAMME

Voltage ratio		Number at various ratings										Group totals		
		75 MVA	70 MVA	60 MVA	45 MVA	30 MVA	20 MVA	15 MVA	10 MVA	7.5 MVA	5 MVA	3 MVA	Number	Capacity
kV													MVA	
132/66		—	—	7	2	4	—	—	—	—	—	13	630	
132/33		5	1	36	36	29	7	2	3	1	—	120	5302.5	
132/20 22 }		—	—	1	—	1	2	—	—	—	—	4	130	
132/11		—	—	—	6	9	6	4	—	—	—	25	720	
132/6.6		—	—	—	—	4	1	3	1	1	—	10	202.5	
66/33		—	—	—	2	—	4	—	—	—	—	6	170	
66/22		—	—	—	1	—	—	—	—	—	—	1	45	
66/11		—	—	—	—	5	2	1	—	2	1	11	225	
66/6.6		—	—	—	—	—	—	3	—	—	—	3	45	
66/5.25 5.75 }		—	—	—	—	—	—	2	—	—	—	2	30	
33/11		—	—	—	—	—	2	5	11	3	4	27	273.5	
33/6.6		—	—	—	—	—	4	3	1	2	—	10	150	
Group totals	Number	5	1	44	47	52	28	23	16	9	5	2	232	
	Capacity, MVA	375	70	2640	2115	1560	560	345	160	67.5	25	6		7923.5

**Table 13**  
**CLASSIFICATION OF GRID TRANSFORMERS IN THE 1953-59 PROGRAMME**

Voltage ratio		Number at various ratings												Group totals	
		120 MVA	100 MVA	90 MVA	75 MVA	60 MVA	45 MVA	37.5 MVA	30 MVA	25 MVA	20 MVA	15 MVA	10 MVA	Number	Capacity
kV															MVA
275/132	83	—	—	—	—	—	—	—	—	—	—	—	—	83	9960
275/66	5	—	—	—	—	—	—	—	—	—	—	—	—	5	600
132/66	—	—	—	—	10	25	6	—	—	—	—	—	—	41	2520
132/33	—	2	2	2	90	106	—	66	—	2	4	—	—	274	12780
132/20 } 22 }	—	—	—	—	3	4	4	7	—	—	—	—	—	18	720
132/11	—	—	—	—	—	—	3	—	36	—	6	10	1	56	1495
132/6.6	—	—	—	—	—	—	—	4	—	7	3	—	—	14	305
132/3.3	—	—	—	—	—	—	—	—	—	—	—	—	9	9	90
66/22	—	—	—	—	—	—	2	—	—	—	—	—	—	2	90
66/11	—	—	—	—	—	—	—	5	—	—	7	—	—	12	255
66/6.6	—	—	—	—	—	—	—	1	—	—	—	—	—	1	30
33/11	—	—	—	—	—	—	—	—	1	—	2	—	—	3	55
Group totals	Number	88	2	2	12	118	121	4	119	1	15	26	10	518	
	Capacity, MVA	10560	200	180	900	7080	5445	150	3570	25	300	390	100		28900

generator protection equipment, placing the main generator relay panel at the generating station and a subsidiary relay panel at the switching station.

Where the site available is unusually restricted or atmospheric pollution is likely to be severe, indoor construction at 132 kV is now employed. A fully segregated arrangement in which each switchgear compartment can be completely isolated for maintenance has been used in four cases, but the "hall" type has proved more economical in cost and its use is being extended. The "hall" construction employs what is substantially an outdoor layout enclosed in a building, the circuits being segregated only by space and wire-mesh screens. The reduction in area occupied compared with the standard outdoor construction is as much as 80% for segregated construction and 65% for "hall" construction.

The 132 kV circuit-breakers employed are substantially equally divided as between bulk-oil and air-blast types. Both types are being used for short-circuit ratings of 2500 and 3500 MVA, but modern installations are invariably for ratings of 3500 MVA. System development has considerably increased the short-circuit duty of many of the original 1500 MVA switchgear installations fitted with old types of bulk-oil circuit-breaker, and they have been modified by fitting new mechanisms, arc-control devices and modified bushings to make them suitable for a rating of 2500 MVA to modern standards.

All the modern 132 kV and 275 kV bulk-oil circuit-breakers purchased by the Authority, together with the old-type 132 kV units modernized for a rating of 2500 MVA, are of the 2-breaker-per-phase type employing resistance switching with one containing tank per phase.

For the 275 kV stations a rating of 7500 MVA is now standard; both air-blast and bulk-oil circuit-breakers are employed, with the air-blast type predominating.

Compressed-air mechanisms are increasingly being used to close bulk-oil circuit-breakers. For the latest air-blast circuit-breakers axial-flow nozzles are in general use, with three or four breaks for 132 kV service and six to eight breaks for 275 kV service. The control of voltage distribution on multi-break air-blast units for 132 and 275 kV service is normally achieved by non-linear resistors or by capacitors.

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## (8) HYDRO-ELECTRIC PLANT

### (8.1) Scotland

Post-war hydro-electric developments in Great Britain have been limited to those of the North of Scotland Hydro-Electric Board. Since its formation in 1943 the Board has completed schemes having a total installed capacity of approximately 350 MW, the average cost being estimated as £114 per kilowatt installed. At the end of 1954 the Board had under construction further schemes with a total capacity approaching 400 MW, while other schemes which have either been promoted and on which construction has not yet started, or which are in course of promotion and survey, are estimated to involve an additional capacity of some 400 MW. Between them these schemes will harness about one-third of the potential water-power resources of the Scottish Highlands. By comparison, the total pre-war capacity was some 190 MW in this area and a further 120 MW in south Scotland. Table 14 surveys the situation which existed at the end of 1954.

Six of the generating stations now building are to be either wholly or partly underground—a technique which has been adopted on the Continent for some years, but which has not yet been used in this country. The reasons for the adoption of this type of layout are basically economic, for in certain locations the underground station is cheaper than the above-ground type.

In the immediate post-war years most of the stations contained two or more sets, as a safeguard against breakdown, but with the increasing number of stations and more complete inter-connection it is now possible to design stations with only one set, with resulting advantages in both simplicity and economy. Six of the stations now building will be of this type and the largest will be of 40 MW capacity.

There has been a general tendency towards the adoption of large sets operating under higher heads, and Francis turbines have been designed to operate under heads which a few years ago would have been limited to Pelton wheels. Thus, at Sloy, four 32.5 MW vertical Francis turbines have been operating for some years under a gross head of 910 ft, and for the Clachan station a 40 MW Francis turbine is being designed for a head of 965 ft. At the Invergarry station, at present under construction, a 20 MW

Table 14  
ANALYSIS OF PROGRAMMES OF THE NORTH OF SCOTLAND HYDRO-ELECTRIC BOARD

Scheme and stations	Number of sets	Set capacity	Gross head	Type of turbine	Catchment area	Average annual output	Average annual load factor
		MW	ft		miles <sup>2</sup>	MWh × 10 <sup>3</sup>	%
<i>Stations in operation at the end of 1954*</i>							
Loch Sloy	4	32.5	910	Vertical Francis	32.5	120	10.3
Tummel-Garry							
Pitlochry	2	7.5	50	Kaplan	706	54	41.0
Clunie	3	20.4	173	Vertical Francis	515	143	26.6
Gaur							
Gaur	1	6.4	92	Vertical Francis	93	17	30.3
Affric							
Fasnakyle	3	22.0	522	Vertical Francis	124	233	38.6
Fannich							
Grudie Bridge	2	12.0	537	Vertical Francis	56	83	39.5
Glascarnoch-Luichart-							
Torr Achilty							
Luichart	2	12.0	185	Vertical Francis	329	124	58.8
Torr Achilty†	2	7.5	52	Kaplan	345	36	27.4
Cowal							
Striven	2	3.0	400	Horizontal Francis	15	14	26.7
Lussa							
Lussa	2	1.2	380	Horizontal Francis	13	8.5	40.4
Gairloch							
Kerry Falls	1	0.25	185	Horizontal Turgo	13	3.5	32.0
	2	0.5		Horizontal Francis			
Lochalsh							
Nostie Bridge	2	0.625	490	Horizontal Turgo	7.3	4	36.3
Storr Lochs							
Storr Lochs	2	0.95	447	Horizontal Francis	5.2	3.5	21.0
Loch Dubh (Ullapool)							
Loch Dubh	2	0.6	543	Turgo	5.1	5.5	52.3
Morar							
Morar	2	0.375	16	Kaplan	65	2	30.5
Tobermory							
Tobermory	1	0.08	140	Horizontal Francis	—	0.6	24.5
	1	0.2		Horizontal Francis			
<i>Stations under construction at the end of 1954</i>							
Tummel-Garry							
Errochty	3	25	610	Vertical Francis	86	103	15.7
Affric							
Mullardoch Tunnel	1	2.4	82	Vertical Francis	124	8	38.0
Shira							
Sron Mor	1	5.0	160	Horizontal Francis	13	80	20.3
Clachan	1	40.0	965	Vertical Francis	21		
Glascarnoch-Luichart-							
Torr Achilty							
Glascarnoch	2	12.0	529	Vertical Francis	102	112	53.2
Torr Achilty	2	7.5	52	Kaplan	345	36	27.4
Achanalt†	1	2.0	65	Kaplan	73	8	45.6
Lawers							
Finlairg	1	30.0	1362	Pelton	17.5	80	30.5
Garry							
Quoich	1	22.0	320	Horizontal Francis	52	77	40.0
Invergarry	1	20.0	175	Kaplan	151	82	46.8
Moriston							
Ceannacroc	1	16.0	296	Vertical Francis	79	70	40.0
Glenmoriston	2	16.0	306	Vertical Francis	148	114	40.6
Kilmelfort							
Kilmelfort	1	2.0	365	Horizontal Francis	11	9	51.3
Alt-na-Lairge							
Lairge	1	6.0	817	Pelton	5.4	20	38.1
Breadalbane							
Lochay†	2	22.5	592	Vertical Francis	89	156	39.6
Lubbreoch†	1	3.0	92	Kaplan	41	10	38.1
St. Fillans	1	21.0	830	Vertical Francis	41	76	41.3
Dalchonzie	1	3.5	91	Vertical Kaplan	91	18	58.7
Loch Shin							
Lairg	1	3.5	26	Vertical Kaplan	234	10	32.6
Shin	2	12.0	265	Horizontal Francis	250	103	49.0
Cassley	1	2.5	372	Horizontal Francis	24.7	24	27.4
	1	7.5		Horizontal Francis			

\* Constructed by the Board since its formation in 1943.

† One set in operation.

‡ Not yet installed.

Kaplan turbine is being installed for a gross head of 175 ft, which is extremely high for this type of machine.

### (8.2) England and Wales

In England and Wales the total installed capacity of hydro-electric plant before the war was 54 MW, most of it concentrated in three stations in North Wales. Since the war, hydro-electric development in these two countries has been limited to catchments extensions at two of the North Wales stations, an additional 10 MW machine being installed in one of them. The Central Electricity Authority obtained powers to carry out this development in 1952, and construction is now proceeding.

At present the Authority is seeking powers to construct a 49 MW hydro-electric scheme on the Afon Rheidol, near Aberystwyth, and a 300 MW pumped-storage scheme at Ffestiniog, in North Wales; this would be the first large pumped-storage scheme in the British Isles.

### (9) BUILDINGS AND CIVIL WORKS

The economy of superstructure construction may approximately be assessed by the enclosed volume and the weight of structural steel used per unit of installed capacity; under independent development, figures of 60–65 ft<sup>3</sup>/kW and 40–50 tons/MW were encountered in stations using four boilers for two turbines.

These figures have been drastically reduced by a combination of careful design and the adoption of one boiler per turbine. Building volumes of 30, 60, 120 and 200 MW plant are now about 42, 32, 26 and 24 ft<sup>3</sup> per kilowatt of installed capacity when boilers are completely housed, or about 17 ft<sup>3</sup>/kW or less when semi-outdoor boilers are installed. The steel consumption at the larger stations has been reduced to about 15 tons/MW, and if the generator stators are handled by means of special lifting tackle, further small reductions can be made. Reinforced-concrete construction was adopted for turbine rooms at a number of stations when structural-steel sections were scarce.

The turbine room and mechanical annexe are fully enclosed at all stations. Opinions about semi-outdoor boilers are still divided, it being contended that the increased cost of protecting the boiler and its ancillary plant against severe frost leads to very small price differences compared with cheaper boiler and ancillary plant and a lightly clad boiler house. Semi-outdoor boiler operating platforms may, however, embody solid sections at strategic locations to protect operators during inclement weather.

At some stations further economies in superstructure construction have been achieved, while still preserving architectural features, by using brickwork for the lower walls and a lighter form of cladding for the upper sections. This type of construction also releases large quantities of bricks for building houses.

The cost of the station substructure is governed by subsoil conditions at the site, and will be high if it is necessary to provide many nests of long piles, capped at their upper extremities and interconnected by longitudinal and transverse beams to support the foundation raft and localized heavy plant loads.

Many large generating stations located on nearly-constant-level fresh-water reaches of medium-size rivers have been provided with mixed circulating-water cooling systems. When the flow in the river is equal to or greater than the circulating-water requirements of the station, natural cooling is used, but when the water level is low, cooling towers are arranged to take varying fractions of the total cooling duty. Some 70–80% of the total circulating water used would then pass through the cooling towers, but the towers would be used only for a limited number of hours per annum, dependent on the shape of the river-flow duration curve.

Natural-circulating-water systems associated with estuary sites, where water is plentiful but may be subject to level variations of

up to 40–50 ft, may involve very expensive civil works, particularly when the subsoil conditions are bad.

At large inland stations, where the fuel is brought by rail and consumed at the rate of 3 000 000 tons per annum, or 10 000 tons average and 12 000 tons maximum per day, extensive and costly civil work is incurred in the provision of railway sidings and associated wagon tippler pits and handling plant. Outside the station boundary and adjacent to the main-line railway it is necessary to provide a reception siding comprising one arrival, one departure, and one locomotive-turn-round track. Within the station boundary the capacity of the exchange sidings is at present based on providing the equivalent of one day's full-wagon and one day's empty-wagon standage for the maximum daily fuel consumption. The average track in the exchange sidings is suitable for trains of about 64 mixed-capacity wagons; with wagons of 12.5 tons mean capacity each track holds 800 tons of coal, so that a maximum consumption of 12 000 tons a day demands 15 tracks for full wagons and 15 tracks for empty wagons. However, since each track would accommodate about 50 wagons of 24 tons capacity, their use would reduce the number of tracks required to 20 and thereby enable material civil and mechanical savings to be made.

Harbour and estuary sites require extensive and costly civil works to provide harbour facilities and/or jetties capable of the simultaneous unloading of colliers of 4 000 tons burden. At many sites the stocking out and reclaiming of large quantities of fuel is being done by means of carry-alls and bulldozers, to save expensive civil and mechanical construction.

### (10) FUTURE PLANS

An increasing need to conserve the fuel resources in this country by improved methods of fuel utilization in every field of national activity, including the generation of heat and power by nuclear means, has already had repercussions on the Central Electricity Authority's generating-plant and station programmes from 1960 onwards.

There is a grave danger that, unless drastic action is taken to conserve traditional fuel, a serious shortage may develop in this country within little more than a decade, for by that time the estimated combined industrial, domestic and power-generation fuel demands will exceed the anticipated coal production. It has thus become necessary to investigate schemes for conserving fuel, the use of oil to supplement coal supplies, and the incorporation of nuclear-power generation projects in the Authority's later plant programmes.

The completion, in 1961, of the last 13 power stations in which standard 60 MW 900 lb/in<sup>2</sup> straight condensing sets were to be installed implied the inclusion of a further 26 sets in the 1960 and 1961 programmes, but to ensure a material conservation of fuel and to minimize the increase in the cost of generation despite an 18% increase in the cost of fuel, these will now be substituted by 13 standard 120 MW 1 500 lb/in<sup>2</sup> reheat sets. Although the same total installed capacity of nearly 4 000 MW will be available from these stations at the end of 1961, about 40% of the total will consume 11% less fuel, representing savings of at least 350 000 tons per annum and £7 000 000 in capital expenditure. The change will be accomplished by accelerating the installation of 60 MW sets at eight complete stations and one half-station to compensate for a delay in the commissioning of the first 120 MW high-efficiency sets at four complete stations and one half-station before the end of 1960. Under this scheme the maximum deficiency of total plant installed in 1958–60 will be limited to 280 MW in 1960 and will be removed by the end of 1961. Moreover, the use of standard 120 MW plant limits the delays mainly to the changing of civil-engineering and building drawings, and obviates the development of special

60-80 MW reheat sets until they are required for the reconstruction of old existing generating stations.

Further hydro-electric projects being investigated include the Severn Barrage scheme, supplemented by a high-head pumped-storage plant to ensure a firm supply at times of peak demand. Other pumped-storage schemes are being investigated for use in conjunction with high-efficiency steam or nuclear plant.

Many combined commercial process-steam and power-generation schemes for industry have been examined, and a project for supplying about 600 000 lb of process steam per hour from a high-pressure high-temperature plant will soon enter the construction stage. In accordance with the 1947 Electricity Act, many new district-heating schemes incorporating thermal generation have also been investigated in association with various local authorities and others, but none has yet been proceeded with, for economic reasons.

There is little to report in respect of three large open-cycle and one closed-cycle experimental oil-burning gas turbines. One closed-cycle and one open-cycle plant have entered the experimental running stage, and the remaining two are under construction. On the Continent, six 25 MW oil-burning gas turbines are on order or under construction; they are 2-line machines with no exhaust-heat exchanger, the high-pressure high-speed line constituting the supercharging element and the low-pressure 3000 r.p.m. line the power element. Several small special gas turbines sponsored by the Ministry of Fuel and Power are either under construction or entering the experimental stage. One will operate on a 1% methane gas concentration contained in the upcast air from a mine. Successful short runs have been made in peat-burning plant.

Large quantities of oil to supplement coal supplies will be used at a number of large stations, mainly located on waterside sites and near major oil refineries. The boilers are being changed over to oil burning to enable the maximum amount of oil to be consumed with a minimum of capital expenditure on oil storage equipment, preparation plant and transport facilities.

A rapid development of nuclear-power generating stations will, as stated in the Government's White Paper, Cmd. 9389, make a major contribution to relieving the envisaged serious national coal shortage. It should be realized, however, that to meet the growing demands of the industrial and domestic sections of the community, it is proposed that supplies of coal to the C.E.A. and other generating authorities are to be held at a static level from the early 1960's.

The Central Electricity and Atomic Energy Authorities are collaborating with plant manufacturers in the development of three improved types of gas-cooled carbon-moderated thermal reactors, 1C, 11C and 111C respectively, and also in a water-cooled water-moderated thermal reactor, type 1W. Twelve stations are provisionally planned for commissioning in 1961-65, their combined capacity being about 1 500-2 000 MW and their annual output equivalent to that produced by  $5-6 \times 10^6$  tons of coal per annum consumed in normal stations. The group will comprise two stations each with two reactors of type 1C, two stations each with two reactors of type 11C, four stations each with one reactor of type 111C, and four stations each with one reactor of type 1W.

The Central Electricity Authority has also made tentative plant extension programmes for 1960-70 and a forecast of the 1976 position, on the supposition that nuclear plant will then be available and as reliable as existing steam plant and will operate under base-load conditions for economic and technical reasons. These preliminary surveys indicate that during 1960-70 some 19 000 MW of existing types of steam, and 5 000 MW of nuclear plant might be installed. By 1971 the generation of electricity by nuclear plant would be equivalent to that produced by

$15-18 \times 10^6$  tons of coal per annum consumed in normal steam stations, and this would rise to  $35-40 \times 10^6$  tons per annum in 1976 if 10 000-12 000 MW of nuclear plant were then in service.

The surveys indicate that the national planning pattern of future generating plant will be influenced by four major factors.

First, by 1975 nuclear plant will have absorbed all the possible base-load demand for electricity then available. Any additional plant would therefore have to be designed for 2-shift operation before it could comprise either a major proportion or the whole of the plant required to provide the annual increase in national load plus the replacement of obsolete steam plant.

Secondly, from 1960 onwards nearly all the normal steam plant should be built to operate on high-pressure high-temperature reheat cycles, so that the maximum amount of electricity may be generated from the minimum quantity of coal.

Thirdly, a review of the 15-year nuclear-reactor development programme indicates that, in the first 10 years, 16 thermal reactors totalling about 1 750 MW capacity are to be commissioned; they may be divided into four groups, three comprising progressively improved gas-cooled carbon-moderated types and one of the water-cooled water-moderated type. In the subsequent five years, some 3 250 MW of capacity may also include examples of steaming, homogeneous and fast-breeder types. In view of the magnitude of these programmes, and to safeguard electricity supplies should the early nuclear plant not prove as reliable in service as existing steam plant, an agreed proportion of the nuclear plant included in the two programmes should be regarded as of an experimental nature and not as firm capacity.

Fourthly, as an additional safeguard during the first critical years of commissioning large blocks of nuclear plant, it would be advantageous to increase the proposed static level of coal supplies to all the generating authorities, either by increased mining effort at home or by importing coal or oil. This would enable any new additional firm capacity of steam plant ordered and some older plant held in reserve to be operated in emergency to cover nuclear-plant outages. During the next decade much could and should be done to improve the efficiency and reduce the fuel demands of the industrial and domestic sections of the country, and thus permit them to share in successfully launching a nuclear era.

The trends of mechanical engineering outlined in this review indicate that, in planning as a comprehensive whole the installation of more than 18 000 MW of plant, the main objective has been the rationalization of individual set capacities for operating under either straight condensing or reheat cycles of increasing pressure and temperature. Furthermore, that in spite of all the difficulties of the war and post-war periods a large proportion of the plant directed in the second programme will operate on the unit principle, when consuming low-grade and difficult fuels, with material savings in capital expenditure and running costs to the ultimate benefit of the consumer.

In the development of future nuclear generating plant with the assistance of the Atomic Energy Authority many new steam, and possibly gas innovations associated with steaming, homogeneous and fast-breeder reactors will have to be investigated; but a number of steam-turbine components will be similar to those already incorporated in large set designs. Special attention will have to be given to the detail design and characteristics of both solid-fuel-fired boilers, or combined reactor and heat exchangers for nuclear plant, together with steam-turbine constructions to enable either type of plant to operate under future 2-shift conditions. Every credit is due to the staffs of the supply industry, of the plant manufacturers and the Atomic Energy Authority for the enterprise and energy without which these contributions to the rehabilitation and future prosperity of our country would have been impossible.

## DISCUSSION ON 'A BRUSHLESS VARIABLE-SPEED INDUCTION MOTOR'\*

SOUTH MIDLAND SUPPLY AND UTILIZATION GROUP, AT BIRMINGHAM, 10TH JANUARY, 1955

**Dr. E. Friedlander:** In the course of the London discussion on this paper, one of the speakers proposed to call this new machine a "wedge" motor. This name does not do full justice to the basic principles involved. I would rather call the machine a "sail effect" motor. A sailing boat is actually capable of running faster than the wind exactly as this motor is capable of running faster than the field which drives it. The explanation in the case of the sailing boat is based on the different resistance the boat offers to water if moving forward as compared to the much greater resistance to be overcome for lateral movements. The same applies to the new motor, which would not be able to run faster than the synchronous speed without the unequal resistance and impedance with reference to the two axes of velocities. I have already drawn attention to the mathematics of this with reference to the authors' eqns. (1), (3) and (5) in my contribution to the London discussion.

I have also mentioned before that the influence of leakage should be beneficial if it can help to reduce preferentially the current in that axis which contributes the useless brake torque. Another conceivable solution may take advantage of the principle used in high-torque motors, namely that of rotor resistance varying with frequency. The currents due to the radial component of the travelling field having a greater velocity relative to the disc could, perhaps, be reduced if the effective rotor resistance could be given a frequency-variable value such as occurs in deep slots, surface resistances of steel and other solid conductors.

I cannot quite agree with the reference to the end effect in the stator field as explained by the authors in the last paragraph of Section 3. This consideration is somehow misleading, because the travelling flux is not a magnetic field which is literally generated at the beginning of the array and which disappears at the end. The only condition which must be satisfied is that the sum of all alternating fluxes should be zero at any time, and this is undoubtedly the case without any considerations of end effects. The travelling character of the field may rather be compared with the optical illusion involved in observing a rotating screw. Whatever end effects may be observable in local rotor current densities may be explained by leakage fluxes helping to crowd the rotor currents at close distance from the stator overhang.

**Mr. H. Fricke:** I should like more information on the power factor of the equipment demonstrated, and also on the horsepower available from the disc.

It seems that additional coils around the periphery would give an output of commercial interest.

**Mr. W. P. Richardson:** From the dimension given in Section 4 and the fact that, as shown by the continuous curves in Fig. 13, the speed for zero torque at 600 c/s is 1200 r.p.m., the spacing of the four stator poles on the experimental machine gives a field speed equivalent to 60 poles. It is noticeable that at synchronous speed and above the torque falls away appreciably, whereas the usual demand in industry is for a "stiff" machine with a small slip.

It would be interesting to know whether the no-load speed indicated by the broken curves is the maximum obtainable, and also to see the power-factor curves corresponding to the curves shown.

There are one or two possible modifications to the arrangement which the authors may have already considered: first, to arrange for the stator to be moved radially in a slot so as to give the effect of a varying number of poles or field speed. This could be combined with the angular movement of the stator and the use of a copper disc as described in the paper, and the result would, I think, be to make the torque characteristic more stable over a range of speed. Alternatively the radial movement of the stator could be used with a steel disc with copper rivets to form a motor with the equivalent of variable pole-changing within the limitations of the disc radius. Such a machine would probably have a higher power factor and be more efficient.

**Mr. J. R. Anderson:** Induction motors can usually be operated as a form of generator so I have been trying to determine whether this machine would function in a similar way. I should be glad if the authors could indicate whether it would operate in this manner and, if so, what would be the effect of rotating the poles.

**Mr. J. E. Brown:** I feel that the authors have created the alternative presented by eqns. (1) and (2) by asking, in effect, "What does one resolve?" Since the motion of the field with velocity  $v_s$ , in the direction shown in Fig. 2, is "ideally" the only cause of motion of the sheet, the answer to the question of how fast the sheet will now travel is, by Lenz's law, "With a velocity component  $v_s$  in the direction of motion of the field." Hence eqn. (2) is valid. In practice, for  $\theta > 0$ , neither equation is valid, because of the braking force discussed later in the paper. If eqn. (2) cannot be accepted, ideally, on simple theoretical grounds, it can hardly be confirmed by experiment. The initial experiments described in Section 2 are more important for bringing out the significance of the braking force.

**Dr. D. A. Bell:** The authors state that in the more elaborate form of the machine the reactance of the rotor plays an important part in reducing the braking effect due to the component of field which moves at right-angles to the direction of motion of the rotor surface. This is clearly due to the fact that near synchronous speed the slip frequency of the parallel-to-motion component is small enough to make reactance ineffective, while the perpendicular component has a high enough frequency to be impeded by reactance. Would not the same argument apply to eddy currents in a machine with a disc rotor, if the thickness of copper were several times the depth of penetration for eddy currents at the supply frequency? Are the authors sure that the increased efficiency obtained by studding the disc with iron is more due to the reduction of losses associated with the magnetizing current of the field system than to the increased reactance of circuits in the rotor?

**Dr. R. D. Gifford:** It is clear that one has to encourage the driving currents in the disc and discourage the braking currents. I assume that these currents flow at a considerable angle to each other, and I wonder whether it would be possible to put saw

\* WILLIAMS, F. C., and LAITHWAITE, E. R.: Paper No. 1737 U, November, 1954 (see 102 A, p. 203).

cuts in the disc, so placed as to allow the driving currents to flow and at the same time to impede the braking currents.

**Mr. J. B. Brockbank:** The construction used for the disc-type rotor with its multitude of iron rivets is hardly a commercial proposition, and there are obvious difficulties in the production of a spherical rotor, with conducting paths in all directions, and at the same time with sufficient iron to give reasonable flux paths. I wonder whether it might be practicable to use a

sintered-metal technique, with suitable proportions of iron and copper in the mixture, to give a reasonable combination of conductivity and permeability in all directions.

**Messrs. D. P. Sayers, R. Paterson and A. R. Wade** also contributed to the discussion at Birmingham.

[The authors' reply to the above discussion will be found on page 208.]

#### NORTH-WESTERN UTILIZATION GROUP, AT MANCHESTER, 15TH FEBRUARY, 1955

**Mr. J. N. Legate:** Can the authors indicate the probable power factor for a practical design of spherical-rotor machine, and how the power factor is likely to vary with stator angle?

Also, although the pivot of the stator could no doubt be arranged so that there is no turning moment under steady-state conditions, is there likely to be much torque on the stator during acceleration and deceleration? It seems that there will be a lag between stator position and speed? The point might be important for automatic or remote-control applications.

**Mr. W. Hill:** When I was asked about a year ago to make a quick appreciation of the authors' new machine, it was obvious at once that there was no point in considering the disc type of rotor. Instead it was necessary to use the normal type of laminated rotor made up of a large number of sheet-steel punchings having rotor bars in both directions. In the design of rotating machines generally one is very conscious of the need to employ high-permeability material to get as much of the magnetic energy as possible to the right place in the air-gap with the minimum expenditure of m.m.f. The next step in trying to find out how this machine behaves was an endeavour to obtain an equivalent circuit. This is more accurate and flexible than its graphical counterpart, the circle diagram, especially with freak machines.

Fig. C shows the usual equivalent circuit of an ordinary

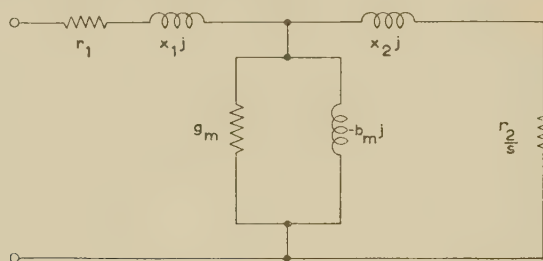


Fig. C

induction motor, still applicable to this new machine, but with rotor parameters which have to be interpreted in a new manner.

However, before tackling the rotor itself, I met quite a snag in the determination of the magnetizing reactance. In order to make this clear the new machine is re-drawn in Fig. D with sheet-steel punchings in both the stator and rotor, and with zero angle between the stator unit and the axis of rotation. In this case the magnetic flux of the stator will have to cross only the actual air-gap between the stator and rotor. For the sake of clarity the stator and rotor are shown separately in Fig. D.

As soon as the stator unit is turned through an angle  $\theta$  the flux must cross an additional air-gap, as shown in Fig. E, the additional gap corresponding to the length A-O.

The analytic expression for the extra path, in terms of the angle  $\theta$ , the thickness of the punchings,  $t$ , and the clearance between the punchings, and the pole pitch  $p$  is given by the expression

$$\frac{p\delta \sin \theta}{t + \delta}$$

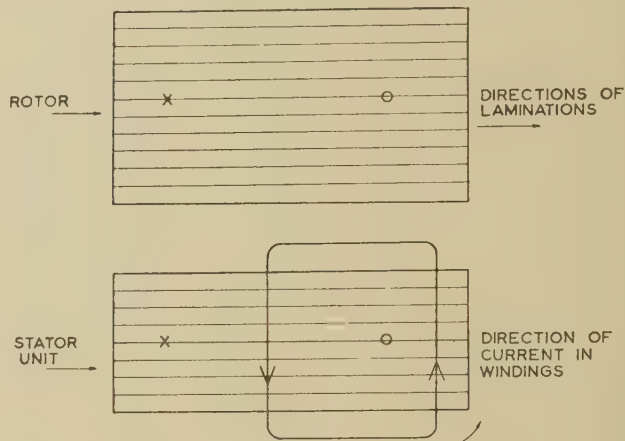


Fig. D

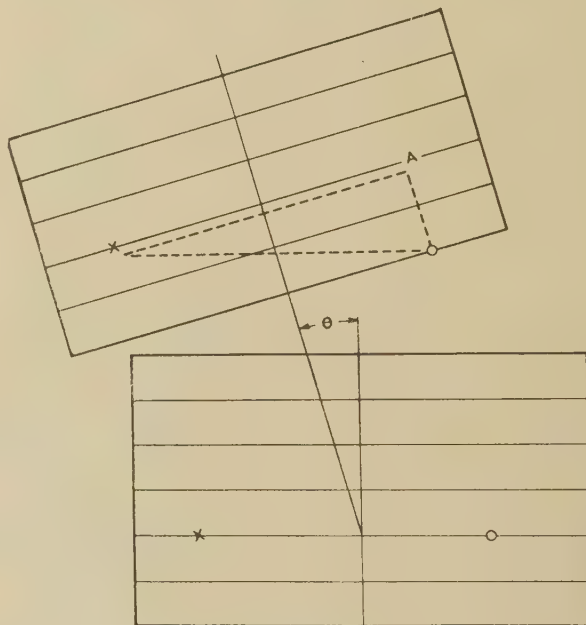


Fig. E

This is by no means a negligible increase and may multiply the magnetizing reactance by as much as a factor of ten for moderate angles and a pole pitch of, say, 6 in. No wonder the authors have kept their number of poles very large.

The increase in magnetizing reactance does not necessarily mean a corresponding increase in magnetizing current because of the increased voltage drop caused by the magnetizing current in the stator impedance. There is therefore a very much lower voltage across the rotor circuit than with the normal magnetizing reactance alone. This might well explain some of the authors' unaccounted losses. None of the published curves shows this effect.

**Mr. C. Ayers:** I have for some time been interested in obtaining motors having variable speed characteristics when supplied with electrical energy from a constant-frequency source. Such characteristics can and have been obtained without recourse to a commutator, although the more elegant and commercially profitable forms of obtaining these characteristics do employ this device. The commutator, in such circumstances, as in all its applications, is used for its inherent properties as a frequency changer, whether it be changing from supply frequency to slip or other frequency, or from rotational frequency to zero frequency (direct current).

Further, as is well known, the more usual form of variable-speed motor possesses a fair number of poles and to a large extent operates on multiples of three phases. This means that numerous brushes are required, which appear to have, under the circumstances of operation, a life of very uncertain length, demanding replacement and maintenance, the cost of which may prove economically unacceptable; from these points of view the abolishment of brushes is to be welcomed.

It is well known from the classical output equation that the power output of a machine depends on several factors, including the  $D^2L$  of the machine, the mean gap flux density, the current loading of the rotor, the rotational speed and the operation power-factor. In a given machine of determined  $D^2L$  the output can be raised by the manipulation of three factors, namely flux density, current loading and speed.

The flux density can be increased by the provision of a low-reluctance magnetic circuit, and the current loading can be improved by the provision of low-conductivity paths for the flow of rotor current. These two factors do not seem to be complementary in the present conception of the machine, in that the possible, complete, nature of rotor current requires not only good longitudinal paths, but owing to rotor movement, good paths at various angles to the rotational axis of the machine. This appears to need a completely cylindrical rotor winding, which is tantamount to an increase in the air-gap of the machine, thus increasing the reluctance of the magnetic circuit and adversely affecting the power factor of the machine. The latter point applies particularly to the disc-pattern machine, as I assume that in the two stators used, north pole was placed opposite north pole, giving a poor magnetic circuit and at the same time producing a poor stator power-factor, owing to leakage.

The third factor, namely speed, appears to lend itself to variation to increase the power output since power is proportional to speed.

If we assume, in the new machine, that the pole-pitch/pole-width ratio is  $R$ , and that the ratio of pole width to rotor radius is unity, as in the barrel machine, we obtain the result that the speed of rotation is proportional to  $R/\cos \theta$ . For practical purposes we can obtain a speed range of 2 : 1 by variation of  $\theta$  from  $0^\circ$  to  $60^\circ$ , over which range for usable values of  $R$  speed varies directly as  $\theta$ . If a greater speed range is required, the parameter to vary appears to be the frequency, which unfortunately raises the problem of a variable-frequency supply.

As a last point I would ask the authors whether they have as yet developed the general circle diagram of the machine, which may be of help in determining its inherent properties with a view to assessing their importance in any line of development.

**Mr. G. G. Scarrott:** The chief problem which remains to be solved in the design of the variable-speed induction motor is to raise the efficiency. It is arguable that the low efficiency obtained is inherent in the linear induction motor. The argument which suggests this can be put in two different forms. In crude physical terms the torque of an ordinary induction motor can be considered to be an interaction between a magnetic field linking the squirrel-cage winding and currents induced in the squirrel cage

as a result of the slip. In terms of this picture one can say that the induction motor is efficient because the rotor winding can be made of such low resistance and can be so tightly coupled to the stator that the relaxation time for changes in the magnetic field linking the rotor can be made very long compared with one period of the stator supply.

If one now tries to adapt the same design principles to a linear induction motor, one is faced with a dilemma. If the linear squirrel cage is made of low resistance, the portions of the squirrel cage which happen to be under the leading end of the stator pass large parasitic currents as the stator attempts to create a magnetic field linking the squirrel cage. Similarly, parasitic currents occur at the trailing edge of the stator, since the magnetic field has to be destroyed. These parasitic currents in the squirrel cage induce corresponding currents in the stator windings and account for the large loss. If the squirrel cage is designed with a high resistance, to reduce these parasitic currents, the efficiency of the central and most useful portion of the stator is impaired. It follows that a linear induction motor cannot be efficient unless the end regions of the stator can be neglected, i.e. the stator must contain many poles.

The same result can be shown in a more rigorous way as follows: All the properties of the conventional induction motor will be possessed by an infinite linear induction motor where the stator and squirrel cage are both very long and contain a large number of poles. Hence, the properties of a finite linear induction motor can be deduced by Fourier analysis of the stator field into a spectrum of infinite sinusoidal magnetic-field distributions. Thus, at a certain instant when the stator magnetic-field pattern has odd symmetry it can be analysed into a spectrum of sine waves with a variety of different wavelengths. One quarter-cycle later the magnetic-field pattern has even symmetry and can therefore be analysed into a similar spectrum of cosine waves. The moving field of a 4-pole linear stator can therefore be analysed into a band of infinite sinusoidal fields, all with the same frequency, but with wavelengths and linear velocities covering a range of about 3 : 1. It is clearly difficult with any design of linear squirrel cage to make efficient use of such a broad spectrum of fields moving at different speeds. If the stator contains many poles the spectrum degenerates into a narrow band which can be exploited to make an efficient induction motor. Thus the Fourier transform argument leads to the same result as the physical argument.

If these theories are correct they could be developed quantitatively to predict a maximum possible efficiency for a linear induction motor in terms of which the losses of the machine demonstrated may not be so high.

**Mr. H. C. Smith:** There are one or two points in the paper where additional information would help machine designers to assess the real usefulness of the work described in the paper.

First, what was the air-gap of the machine described? Secondly, how was the output measured? It is stated that the estimated efficiency of the first machine was 0.1%. This means that with an input of 50 watts, as for the second machine, the output was 0.05 watt. Can the author throw a little more light on the actual conditions?

There are, of course, many drawbacks to making this type of machine a commercial proposition. The spherical-rotor motor is interesting, and I should like to ask the authors whether any further work has been done on this machine.

It is obvious that if the stator covers only a small portion of the rotor, the "synchronous" speed of the motor will be much lower than for a normal induction motor. This seems to be one of its main disadvantages, since the provision of a high-frequency 600 c/s supply to maintain a moderate maximum speed of 2400 r.p.m. would normally be impracticable.

## THE AUTHORS' REPLY TO THE ABOVE DISCUSSION

Prof. F. C. Williams and Mr. E. R. Laithwaite (*in reply*): In the interval between the publication of the paper and the collection of these items of discussion for reply, much has been learned about the behaviour of the machine. Many of the points raised are dealt with in detail in a paper describing this later work which is in course of preparation. Such points will be dealt with very briefly in this reply. It should also be noted that some of the discussion relates to meetings of fairly recent date where the informal presentation went rather further than the written paper. The contributions of Mr. Hill and Mr. Scarrott are particular examples. They have also made valuable contributions in purely informal discussion.

Dr. Friedlander suggests an alternative name for the motor. His suggestion would be historically appropriate, since the analogy he mentions was one of the first to occur to the authors. It seems fairly certain that all the artifices, such as double-caging, used to provide special characteristics in conventional machines can be applied to the new machine, but each will need investigation under the new circumstances. There is no doubt that some edge effect exists and the rotor surface leaving the stator can be shown to be highly magnetized. The field in the gap may, or may not, be regarded as travelling, but the field outside the gap at the leaving end certainly is travelling, at rotor speed.

We regret that we cannot answer Mr. Fricke's questions—the machine was built for demonstration purposes only and no measurements were made. There is no doubt, however, that with additional stator blocks a useful output could be obtained, but with poor efficiency and with power factor set mainly by stator characteristics, and largely independent of rotor speed.

Similarly, we cannot inform Mr. Richardson about the power factor in Fig. 13, since the machine has been dismantled. The broken curves of Fig. 13 show the performance of the machine with  $\theta = 40^\circ$ , which does not correspond with maximum speed as shown in Fig. 12. The function of copper rivets in a steel disc is not understood.

Mr. Anderson's question about induction generation at an angle can be answered by stating that internal generation will occur at any angle of the stator if the speed is in excess of the theoretical running-light speed. Whether any net generation is observed externally will depend on the losses in the machine.

Mr. Brown's reasoning, or any other reasoning leading to eqn. (2), sounds very convincing to the authors, now. They found the experiment, when performed for the first time, even more convincing.

We have not yet been able to study the effects of varying penetration depth raised by Dr. Bell. It may well be that some advantage can be obtained in this way. We think that the major effect of the studs was in reducing magnetizing losses, but there must also have been some effect due to rotor reactance.

The objection to Dr. Gifford's suggestion of putting saw cuts in the disc is that these would be appropriate to one stator angle only.

Sintered metal, as suggested by Mr. Brockbank, could doubtless be used, but so little is yet known about the behaviour of the machine with structures more amenable to analysis that this possibility has not yet been pursued. There does not seem to be any serious difficulty in making spherical rotors, particularly if the bars are cast in.

Mr. Legate's question about power factor in the spherical machine is difficult to answer. Recent work indicates that there is no fundamental reason why the full-load power factor should not approach the values appropriate to conventional machines of similar pole-pitch. Unfortunately the spherical machine necessarily has many poles and therefore a small pole pitch, with attendant high leakage inductance and magnetizing current. Stator reaction forces have not yet been investigated.

We are very interested in Mr. Hill's estimate of the increase in effective air-gap due to the skewing of laminations. The importance of this can be reduced by using a rotor with a deep core, and by using fairly thick rotor laminations. No experiments concerned with this effect have yet been undertaken. It does not seem to contribute significantly to the untraced losses, which have now been ascribed to excess rotor copper-loss.

Mr. Ayers is incorrect in his assumption that north pole faced north pole in the disc experiment, so the magnetic circuit was not as poor as he thinks. In the spherical machine multi-direction conductivity is obtained in combination with a good magnetic circuit by using a network of peripheral and conventional slots.

It is doubtful whether the circle-diagram concept has any useful application to this type of machine.

Mr. Scarrott puts forward general arguments to show that the efficiency to be expected will be less than that obtained with a conventional machine. Unfortunately, these arguments do not show how much less. His suggestion that a quantitative result could be obtained by Fourier analysis of the flux wave presupposes that the flux wave is known. It has now been found possible to formulate the flux wave, but it is easier to deduce the major characteristics direct from this information rather than to use the Fourier method. The theoretical maximum efficiency of a 4-pole structure is then found to be 80%.

The points raised by Mr. Smith are important to the future development of the machine, and are given much attention in the paper referred to at the beginning of this reply. Any answers that could be given in relation to the particular machines described would be misleading, since these machines were crude demonstration models.

## DISCUSSION ON

### 'THE POSSIBILITIES OF A CROSS-CHANNEL POWER LINK BETWEEN THE BRITISH AND FRENCH SUPPLY SYSTEMS'\*

NORTH-WESTERN SUPPLY GROUP, AT MANCHESTER, 19TH OCTOBER, 1954

**Mr. E. L. Davey:** Comparative abrasion tests on armour materials in air show aluminium alloy and steel to have approximately equal resistance, but in salt water the ratio is approximately 6 : 10 in favour of steel armour. A combined abrasion and corrosion test showed a ratio of 3 : 1 in favour of steel. The use of steel armour results in high electrical losses therein, owing to the higher electrical resistance and the magnetic effects. However, these losses occur in the outer part of the cable and because of the low thermal resistance of the external path the effect on the cable rating is limited. A 0.6 in<sup>2</sup> steel-armoured cable would give the same rating as a 0.5 in<sup>2</sup> aluminium-alloy-armoured cable.

From the economic aspect the saving in material costs by using steel in place of aluminium alloy is greater than the capitalized value of the extra losses, and in consequence of the above points, steel armour would be recommended for single-core submarine cables.

**Mr. F. V. Dakin:** Maximum saving in capital expenditure on generating plant is effected by providing full interconnection capacity, and anything less means a corresponding decrease in the saving. It is difficult to correlate this with the statement that a 200 MVA link would be more economic than a 400 MVA link. Have the authors accepted the diversity between the two maximum demands as the only criterion for assessing the interconnection required, and included no allowance for deviations from the estimates of future load and generation as was done in the case of the 275 kV Grid?

Transfers exceeding 200 MW will have to be avoided by control of frequency, and this implies that the exporting country will have to reduce generation since it is tied with the other. Has any attempt been made to assess the loss in revenue by reducing frequency to keep within the capacity of the cable?

**Mr. S. D. Larcombe:** Considerable expenditure will be incurred in laying a fourth 0.5 in<sup>2</sup> cable and in the intricate switching arrangements necessary to make this available in place of any one of the 3-phase cables which might become faulty. Has consideration been given to the laying of six cables of smaller cross-section to carry the full load required under normal conditions? Should one of the cables fail, the interconnection would be maintained through a half-section of copper and therefore carry a reduced but very valuable load at the time when this was so vital owing to the demand either on the British or French supply system.

**Mr. P. Bingley (communicated):** It appears probable that a d.c. interconnection may be set up on a trial basis in the not too distant future. What major restrictions will the designers of the conversion equipment have to bear in mind? For example, I imagine that simple 6-pulse (per cycle) operation would not be acceptable for an interconnection of any appreciable capacity. On the other hand, the stability of the convertors will almost certainly decrease as the pulse number increases and, furthermore, transformer design becomes more complicated.

**Mr. L. F. Ryland** also contributed to the discussion at Manchester.

SOUTH MIDLAND CENTRE, AT BIRMINGHAM, 1ST NOVEMBER, 1954

**Mr. G. S. Buckingham:** Since by the time the cables are installed the total maximum demand of the two countries is likely to be about 40 000 MW, and may well reach 60 000 MW during the life of the cable, 100 MW seems a very slender link. With d.c. transmission at 200 kV between the conductor and earth, or 400 kV between lines, the capacity is increased to 900 MW by the use of all four cables. Furthermore, it would not be necessary to have the automatic-frequency-control equipment which would be essential to control the a.c. transmission of 100 MW between these two large systems.

For submarine cables with pre-impregnated paper insulation a hollow conductor has been used for the passage of gas, and seems to have a number of advantages. I notice that the French gas compression cable incorporates two pilot wires. Does this have some association with the procedure for locating gas leaks, which must be very important in a cable of this sort? We have had some experience of gas leaks on gas pressure cables in this area, and have found it to be a very slow and laborious procedure—which would be even more difficult in the middle of 26 miles of submarine cable.

**Mr. E. V. Hardaker:** The total annual saving of £600 000 quoted is based on ideal operation of the interconnection, and in practice it would not be possible to achieve this ideal. Can

the authors assess what part of this annual saving may be realized with any degree of certainty? Of the three types of energy transfer by which this saving is to be achieved, one refers to the hydro-electric spill energy which France would export to Britain. Does this mean that at times of light load France is able to meet her entire load from hydro-electric sources and still have some to spare for export? In assessing the economic advantage of this link, was any useful information obtainable from France regarding their experience with the interconnections with other countries which have been established for some time?

Who will be responsible for control of the link flow? In this country there is a national control centre which is responsible for frequency control and load transfers over the interconnectors of the 132 kV Grid. Will control be effected by this centre operating in conjunction with a similar centre in France, or will an independent control unit be established? Although shortage of plant may be a thing of the past in this country if and when the link is established, and although frequency fluctuations may be reduced to much narrower limits than at present, the tests show that a very fine frequency control will be essential. Have the French authorities found automatic frequency control, which has already been established in their country, to be of benefit in this respect?

From figures given in the paper I note that the diversity varies

\* SAYERS, D. P., LABORDE, M. E., and LANE, F. J.: Paper No. 1657 S, March, 1954 (see 101, Part I, p. 284).

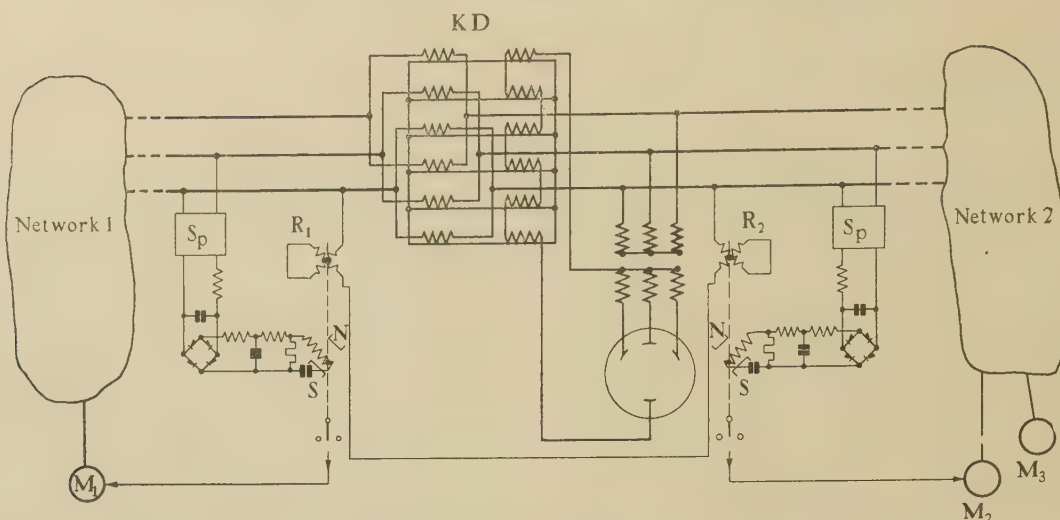


Fig. J.—Power-exchange limitation and control between large networks.

between 400 and 600 MW over the four years quoted. It cannot be said that there is any tendency for the diversity to increase, and it would appear unlikely that a capacity exceeding 300 MW will be required in the foreseeable future. Furthermore, this diversity may decrease if this country followed the French example of making the National time one hour in advance of Greenwich Mean Time.

**Dr. E. Friedlander:** Although the slenderness of the link is certainly a major difficulty, it can be appreciably reduced if advantage is taken of the latest developments in large controllable reactors (transducers). A rigid link can easily be overloaded by a slight swing of the voltage vectors on either side. This would necessitate a very fast frequency control, particularly on the smaller network, to prevent the transmission of power being interrupted frequently. However, if one increases the elasticity by means of a controlled reactor, giving an inherent constant-current control, this makes it possible to let the voltage swing perhaps three or four times as much as with a rigid system. The cost of a reactor of this kind is a very small fraction of the total capital which has to be invested in the cable link.

Fig. J shows a circuit which was first proposed in 1936.\* The magnitude and phase of the voltage across the reactor can serve for an indication of need for correcting power control on either side of the link. When these suggestions were made there was no experience available with reactors of the size wanted for a large exchange of power and there were also economic limitations in the way. Conditions have since changed. A very large reactor (Fig. K) has been built successfully for machine testing purposes, and is about the size required for the cross-Channel link.

A very essential feature of a reactor to be suitable for power requirements is that it should produce no harmonics, in spite of the excessive iron saturation. Fig. L shows the actual current taken with sinusoidal voltage at an output of 75 MVA, and proves that no difficulties need be expected.

There may also be some advantage in using saturated shunt reactors. A cable of the length and voltage under consideration develops an appreciable reactive power. If circuit interruption occurs this will suddenly stress the supply on one end and may locally cause an excessive voltage rise. Highly saturated shunt reactors would be a reliable safeguard in keeping any voltage rise within narrow limits.

**Mr. C. J. O. Garrard:** If 100–200 MW is about the maximum

\* *Bulletin A.S.E.*, 1936, 27, p. 571.

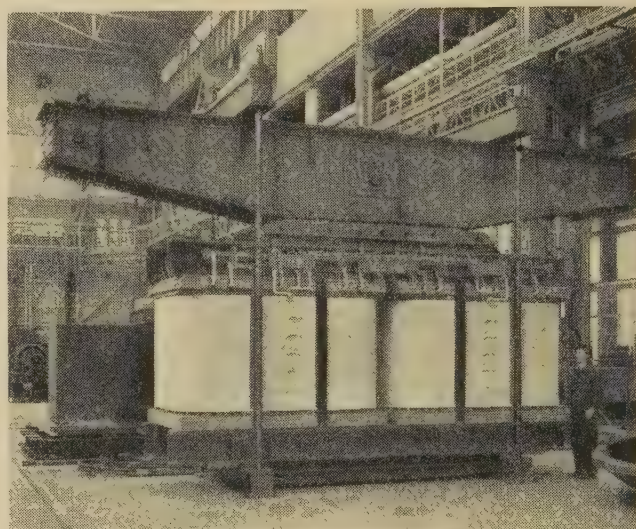


Fig. K.—Test reactor unit outside its tank.

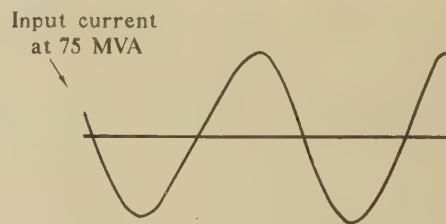


Fig. L.—On-load current waveshape of d.c.-controlled test reactor.

power likely to be transmitted, a cable may very well be the best solution, but if a transfer of the order of 1 000 MW or more has to be considered, I think that a very-high-voltage overhead line would be the only practicable solution. Great advances have already been made in stringing very long conductors, and it should be possible to cross the Channel in, say, four or five spans. The very-high-voltage line could be continued at both ends into the centres of gravity of the load, and thus overcome the difficulty of providing and disposing of the power transmitted through the link.

**Mr. J. H. Patterson:** If the French are not so mindful of the

importance of power factor as we are in this country, the cross-Channel link might be used for the export of reactive current. In this connection I should be interested to know what type of metering equipment the authors would suggest, and what safeguards would be made.

**Mr. R. E. Cornish:** The main justification for the interconnection stems from the diversity of maximum demands on either side of the Channel, and there is no intention that there shall be any large-scale transfer of energy, or rather the intention is that the transfers in one direction should be counterbalanced by transfers in the opposite direction. It is, however, becoming increasingly plain that this country will have to import energy in some form in the future. As we are in the position of having to import coal, is there not a justification for augmenting this interconnection so that, in addition to deriving benefits from diversity in maximum demands, we can also transfer energy during off-peak periods on a reasonably large scale? When the

British 275 kV scheme was under consideration it was established that the cheapest method of transmitting energy in this country was to carry coal by sea, but it seems that the Continental coal-fields are well removed from the sea, and I imagine that quite lengthy rail hauls are necessary to get the coal to a seaport. Therefore, I wonder whether a fairly large scheme of power transmission might be justified.

The paper gives no figures for the amount of plant actually available for peak loads on the French system. We know that there is a large amount of interconnection between the French system and neighbouring systems, so we really ought to know more about the characteristics of the load of those parts of the Continent which are interconnected rather than the French system only.

**Dr. R. D. Gifford, Mr. H. J. Gibson, Dr. K. R. Sturley, Dr. W. G. Thompson and Mr. J. P. Cranmer** also contributed to the discussion at Birmingham.

#### SOUTH-EAST SCOTLAND SUB-CENTRE, AT EDINBURGH, 2ND NOVEMBER, 1954

**Mr. T. R. Warren:** The interconnection of two very large and independently controlled systems by means of a cross-Channel cable of relatively small capacity gives rise to a number of difficult operational problems which it may not be easy to solve. The authors have touched upon these only briefly in the paper and appear to rely upon the provision of some form of close automatic control to ensure satisfactory operation. Some measure of the difficulty can be obtained from the results of the tests referred to in Section 12.2 if we imagine the two systems to be synchronized with the frequencies differing by  $1/60$  c/s, i.e. with the synchroscope needle rotating at a speed of 1 r.p.m. The resultant steady load flow would then be 12.5 MW according to the figures given in the paper. This is undoubtedly an underestimate, because the generating plant in operation in Britain in June, 1953, when the British tests were taken, would be very much less than during the winter, and the French tests were presumably taken with the French system isolated from those of neighbouring countries. It is therefore probably not far wrong to suggest that a frequency difference of  $1/60$  c/s could give rise to a steady load transfer of something like 20 MW.

The normal maximum rate of change of frequency in Great Britain is of the order of  $1/30$  c/s per minute, and on the assumption that the frequencies of the British and Continental systems operating independently tended to vary at this normal maximum rate, the corresponding rate of change of load flow on the interconnector with the two systems in parallel would be of the order of 40 MW per minute. It is very difficult, certainly by hand control, to regulate these large systems to within very fine limits, and automatic control would be essential; but any such control would necessarily require to take the form of combined frequency and load control, in order to maintain a predetermined load on the interconnector within reasonable limits. Has any estimate been made of the cost of providing this control on the British system, and are the authors satisfied that the fine control necessary would not give rise to other operating difficulties?

Even with very sensitive control it could hardly be expected that the normal ebb and flow of power across the Straits could be kept within  $\pm 30$  MW during times of rapidly changing load, and if this is in fact the case, the capacity of the interconnector as a means of effecting planned or intentional transfers

would be materially reduced. The authors have referred to this difficulty in Section 10.2, where they give it as one reason for selecting a conductor section of  $0.5 \text{ in}^2$  for the British cable used in the sea trials, and Table 6 shows that this cable had a rating of 200 MW. Elsewhere in the paper the reference is to a  $0.2 \text{ in}^2$  cable having a rating of 100 MW. Will the authors explain this apparent discrepancy?

**Mr. J. L. Egginton:** The paper indicates that four  $0.73 \text{ in}^2$  cables operating at 400 kV d.c. (line to line) would carry 870 MW. These cables operating with a mid-point earth could therefore be expected to have a firm capacity of some 600 MW, allowing for one cable operating with earth return temporarily in the event of a cable outage. By laying a fifth cable the d.c. network could be given a firm capacity of 860 MW without relying on earth return in case of emergency. In my view this is the likely future development of the cable, and I am in agreement with the authors' proposals to make use of a cable designed for 132 kV a.c. working which is also suitable for 400 kV d.c. working. I think that where 132 kV a.c. is not high enough, the next standard voltage to be used in this country should be 380 kV a.c. or perhaps 400 kV d.c.

The cable would be laid two miles to the west of Dover. While it is unlikely that ships would anchor owing to stress of weather at this point, I think it quite probable that vessels bound up the Channel in thick fog might anchor there to avoid crossing the tracks of cross-Channel traffic to and from Dover. The experience I have had with ships' anchors fouling cables in the Solent shows that the usual procedure is for the ship to heave in the anchor until the cable is in sight and then part it with a hacksaw. One also has to consider the fact that this is the narrowest part of the Channel, where there might be expected to be intense activity during war-time and the possibility of damage to the cable by depth charges, etc. No doubt the authors have considered these hazards and could give some experience based upon the Post Office cables during the last two wars.

No mention is made in the paper of the stability of the interconnection. I should be interested to know what would be the effect of a heavy fault on the 132 kV circuit on the land near Dover, or alternatively a similar fault on the French system near Calais.

#### SOUTH-WEST SCOTLAND SUB-CENTRE, AT GLASGOW, 3RD NOVEMBER, 1954

**Mr. C. W. Marshall:** The background of experience is sufficient to justify confidence in this project. In Britain the 33 kV cable connections between the mainland and the Isle of Wight assuredly

gave warning that it is necessary to proceed with the utmost caution in the Channel project, for the first of the Isle of Wight cables laid during the war under adverse conditions was in-

ordinately expensive. Later circuits were laid not without difficulty but with an adequate measure of success.

The St. Lawrence River cable is particularly interesting because it is over 30 miles long and is laid in a part of the river which is about 1500 ft deep. It might well be asked why a powerful combination like B.E.A. and E. de F. delayed so long when a single Canadian company embarks on a much bigger project without anything approaching the preliminary investigation which has already been done in connection with the Channel cable. The answer is that on the north bank of the St. Lawrence there is plenty of hydro-electric power, and on the south bank there are copper deposits ready to be worked whenever power becomes available. It is in fact a form of 'gold rush.'

There are, of course, numerous submarine power cables in successful service in Scottish waters, the majority being very short and in the bed of the Clyde between Yoker power station and the south bank. The 132 kV crossing at Yoker is overhead, because 132 kV cables did not exist in Britain when the crossing was built. The North of Scotland Hydro Board has also had experience of submarine power cables, and have sometimes had to overcome difficult conditions of the sea-bed and tidal flows.

The Channel cable project bristles with difficulties. Those arising from stormy conditions on the sea are particularly onerous—so much so that it seems well worth while to consider the use of submarines both for examination of the route prior to laying and also for laying the cable.

Detailed examination of the route is essential in the Channel because of the large numbers of wrecks which abound and which would inevitably reduce the life of any cable which might be laid over them. Similar observations apply to underwater cliffs and loose boulders. It would appear that the midget submarine could do the survey admirably.

The conventional procedure of following the example of the telecommunication cable-laying techniques would be inapplicable for a power cable for obvious economic and technical reasons. It would, however, be possible to trail a cable from one shore to the other by means of a submarine, which is protected from the storm conditions encountered on the surface. The capital expenditure involved in providing a special surface vessel is very high, and the money could be more profitably applied to the provision of devices for use in connection with submarines.

#### RUGBY SUB-CENTRE, AT RUGBY, 16TH NOVEMBER, 1954

**Dr. J. C. Read:** The paper constitutes a powerful plea for the development in England of the apparatus necessary for high-voltage d.c. transmission. The essential problem is associated with the high-power mercury-arc valves necessary, and the problems associated with the cables and circuits, although considerable, are much less difficult. The high-voltage steel-tank rectifiers already developed here, if used in the same number and grouping as the valves used in the Gotland scheme, could transmit about 40% of the Gotland power at about 60% of the voltage; this is by no means sufficient, but enough work has already been done in connection with these valves to give a fair idea of the further difficulties to be surmounted and how they could be solved. I think the development of the larger valves necessary for a high-power transmission scheme is quite within the capability of British makers, but it would be very costly.

Numerous supposed d.c. transmission projects throughout the world have been considered by the British rectifier specialists since the war, but with the exception of this cross-Channel scheme, I do not think any of them (in regions where there would be a reasonable prospect of British plant being considered) appear at present to be any nearer coming to fruition than they did 6 or 7

The great advantage of the d.c. link between England and France would be a reduction in cable costs and the much greater degree of security afforded for a large number of cables. Swiss engineers proved that d.c. power transmission by valves was practicable in 1939, and their German counterparts successfully applied the same general principles on a much larger scale in Berlin during the war. They had practically completed a 400 kV, 120 km transmission circuit and terminal stations between Berlin and the Elbe by 1943. It is not unreasonable, therefore, to expect that the rectifiers and inverters for the Channel connections could now be made with complete confidence in their ability to transmit the necessary amount of power between England and France in either direction. The cost of the connection would be of the order of one-third of the cost of a power station per kilowatt of transmitting capacity.

**Mr. H. Marshall:** In the early days of the compression cable the fatigue life of the inner lead sheath was checked by cycle tests on lead strip or pipe, but in both cases the cycle was of very short duration. As a result of these tests it was claimed that the inner sheath had a life of at least 50 years. However, recent work in America has shown that the fatigue life of lead is very dependent on the duration of the fatigue cycle, and on the basis of this work it has been stated that the inner lead sheath of certain compression cables has a life of only 10 years. Since compression cables have been operating in Britain for at least 20 years, it would be interesting to know whether the Committee discovered any evidence of premature failure of the inner sheath during their investigations into the types of cable available.

Doubts have been expressed about the use of metallized paper for the screening of conductors; was there any damage to the conductor screening of the cable recovered from the sea trials?

If a submarine cable of the pre-impregnated type is damaged, one would normally expect moisture to penetrate a considerable distance along the insulation, and in the Vancouver cable, which has a central hollow gas channel, actual flooding of the cable seems possible. Although grappling would, in any case, result in the destruction of a long length of the cross-Channel cable, has any estimate been made of the probable extent of the contamination by sea water and of the effect gas pressure might have in limiting the penetration?

years ago, and some look further away than they did then. The immediate business prospects for d.c. transmission in general are, in my opinion, less rosy than some articles imply. For this reason, and bearing in mind the similar conclusion that has been reached by several prominent firms abroad, I believe that British makers have been right in deferring up to now any serious attack on the d.c. transmission problem. It does not necessarily follow that that is still the right course. It depends on how necessary this cross-Channel link is judged to be.

For this reason, I believe that, if d.c. transmission apparatus is to be developed here, it ought to be developed purely for the special scheme, and not in a vague hope of some other application soon materializing. This policy would be in line with what I think occurred in Sweden, where the practical certainty of a large future demand for d.c. transmission apparatus, for the quite special application that exists there, had a decisive influence on the decision to develop the requisite apparatus in that country. If the electricity authorities here can hold out a firm prospect of a substantial amount of d.c. transmission apparatus being required in this country over a considerable number of years, I have no doubt our manufacturing industry will 'deliver the goods'.

## SOUTHERN CENTRE, AT PORTSMOUTH, 1ST DECEMBER, 1954

**Mr. J. P. Harvey:** In view of the changed military situation as regards the defence of this country, it seems highly probable that within the next two or three years the question of the Channel tunnel may again arise. If this were so, it would be preceded by a pilot tunnel of approximately 6ft in diameter, which would in turn be used to bore the main tunnel. If this tunnel were in existence, the link between England and France could be made by suitable cables of the more conventional type with a consequent considerable saving in cost.

One other aspect that would appear to make the pilot-tunnel scheme more attractive is that a change from 132 to 275 kV for the tunnel cable would surely be very much easier than a similar change with submarine cable, the old submarine cable being of practically no value. The paper appears to indicate that, for an entirely satisfactory link between two such large systems, a 275 kV cable would be of considerably more value in the very near future. Has such an alternative been fully investigated?

**Mr. L. Ananin:** The paper suggests that, with d.c. operation, the cable in question could transmit about 250 MVA. It has been suggested that, if there are no parallel capacitances, the limit of d.c. transmission would be reached at about 50 MW with 12-phase and 58 MW with 24-phase operation with an a.c. system of 3 500 MVA short-circuit capacity. If there is also a cross-Channel a.c. transmission, there will be a considerable capacitance (equivalent to 50 MVA), and the effect of this is to raise the limit to 56 MW with 12-phase and 370 MW with 24-phase operation. Equally it has been suggested that there is no difficulty in providing d.c. transmission for 200–300 MW. I should like the authors' view on these differences of opinion.

**Dr. L. G. A. Sims** and **Mr. M. P. Macon** also contributed to the discussion at Portsmouth, and **Mr. A. Abbott** to the discussion at Bournemouth.

## WESTERN CENTRE, AT BRISTOL, 13TH DECEMBER, 1954

**Mr. W. Hill:** I regret that the 33 kV cables laid across the Severn estuary in 1923 are not mentioned in Table 1. These cables have suffered from being immersed in salt water, from anchors, from a 10-knot current and from bank erosion, and owing to the latter, they have had to be dragged up out of deep sand for repair. From my experience of laying the cables in the Severn and the difficulties of keeping position exactly, I wonder whether it is possible still to obtain paddle tugs, and whether the use of these has been considered, as they are certainly marvellous machines for positioning a boat exactly.

I am also rather surprised that it is decided to coil the cable, as with varying depths of water and varying currents it is extremely difficult to let out a cable at appropriate speed, and obviously there is always going to be a risk of an overstrain, or of the cable rushing with a kink off the coil.

I think that the method used for the Hamel pipe would give the greatest benefits, as this, in conjunction with flexible joints, would ensure one cable operation right across the Channel. The flexible joint should be used as much as possible, but I feel that other joints shown will not be satisfactory. We found it necessary to ensure that there was no possible movement of the core inside the joint, and while this is more important on a 3-core cable such as those in the River Severn, it is still of importance with single-core cable; therefore I think the frame should be extended so that the cable cannot move or twist near the danger area, which is adjacent to the junction of the lead sleeve and the armoured clamps. With the 3-core cable we found the vital distance was  $1\frac{1}{2}$  times the lay of the cable.

Is it proposed to use armoured clamps where the wires splay out considerably, because I can report that, with this type of clamp, although the faces were actually machined accurately, we failed to get a complete grip? With every joint which was examined, it was found that the armouring wires had slipped, although we thought that we had got perfect grip, having pulled up on the armouring wires two halves of the clamps before

putting on any stress. Trials were made with a type of clamp in which the wires are bent over about  $\frac{1}{2}$  in radius right against the lead sheath of the cable, and with this we found that we could ensure taking the complete strain of the cable on the armouring without any slip.

**Mr. W. P. Warren:** While the authors have indicated that there will be a substantial financial saving in commissioning the proposed cross-Channel link, are they reasonably satisfied that such diversity as has existed is in any way a fairly secure figure?

In several parts of the country peak demand has already transferred from the morning to evening period, and with the British system m.d. increasing by approximately 5–10% per annum, the existing diversity of some 400 MW is likely to become insignificant. Even the slightest change in consumer characteristics or weather would be likely to upset the level of this diversity. Would it not therefore be more factual to state that such a link is technically desirable and will provide much useful information, but that the financial saving resulting from such a connection is fairly nebulous?

With the French and British systems interconnected it is natural that there would be consultation between the two Grid controls prior to any switching; but in the event of an emergency it is essential for instructions to be given by one individual only. Would this final authority rest with Britain or France?

**Mr. L. B. Law:** In Fig. 1 the British curve appears to relate to potential loads, i.e. corrections have been made for the effects of low frequency, low voltage and load shedding; no doubt the French curve has a similar basis. Will the authors confirm this? I note that the British maximum demand shown at the end of 1953 is less than the peak for the previous winter. This is due to the fact that the 1953–54 potential peak did not occur until 2nd February, 1954, which is outside the period covered by this chart. The actual figure was 16 309 MW.

**Mr. E. Hywel Jones** also contributed to the discussion at Bristol.

## NORTHERN IRELAND CENTRE, AT BELFAST, 14TH DECEMBER, 1954

**Major E. N. Cunliffe:** One of the criteria concerning the design of the cable stipulated by the authors is that local underwater damage to the outer sheath should not result in contamination of the insulation by sea water along an excessive length of the cable. This is undoubtedly a most important factor; and yet both the gas-filled and oil-filled cables of normal design suffer

from this defect in comparison with the mass-type cable because of the presence of the pressure ducts which are contiguous with the insulation along the whole length of the cable, and in the event of a hole in the sheath, the safety of the cable would appear to depend entirely on the continuous pumping in from both ends of sufficient oil or gas to maintain an outflow at the point

of rupture. Should any hitch occur in this operation before the cable was repaired, water might enter and ruin long lengths of the cable.

One design which overcomes this difficulty is the external compression cable with its inner and outer sheath, and this would appear to be a more suitable type for the present purpose than the one chosen by the British authorities. It is interesting to learn that the French authorities are including it in their trials, although not apparently for this reason, but rather because a higher working stress is claimed for this design.

It would be advisable to check the comparative vulnerability of the various designs by creating artificial underwater damage during the trials and assessing the extent of the travel of sea water along the cable.

**Mr. W. Szwander:** In the light of the studies reported in the paper, the old idea of an interconnection between the power systems in Northern Ireland and Scotland becomes more feasible. Such interconnection would basically differ, however, from the one intended across the English Channel, in that its chief purpose would be unidirectional transport of energy generated in thermal stations adjoining the Scottish coalfields. It follows that a comparatively higher reliability standard would be essential, and either five single-core cables (with two acting as spares) or two independent circuits would have to be provided. Apart from the chief economic justification—that of a considerable difference in the price of coal in Northern Ireland and at the pitheads—additional advantages could no doubt be derived from pooling of standby generating capacities, from using stations with higher efficiencies and through benefiting from load diversities, which, although not necessarily apparent when comparing the Northern Ireland load with that of the British system as a whole, obviously exist between Northern Ireland and the component parts of the British System. It is an entirely different matter, of course, whether an investment on a submarine cable would be justified, in view of the theoretical possibility of power generation in Northern Ireland within the next decade in atomic stations.

From the comparative data for the British and French power systems contained in the paper, it appears that in France the amount of installed generating capacity per megawatt of maximum demand is more than 50% higher than that in Britain. In addition, the mileage of the primary transmission lines in France is at least double that in Britain; this must affect adversely the cost of electric energy in France, and it would be interesting to have some information on this subject.

Finally, does the much better load factor in France indicate a comparatively higher degree of domestic electrification in Britain, including excessive use of electric space heating?

**Dr. D. S. McIlhagger:** Since single-core cables have been chosen and their spacing must necessarily be very great, the intervening medium being conductive, an unusual combination of line constants would appear to result. When the cables are supplying an a.c. load, reverse currents will be induced in the sheath, armouring and surrounding medium, and will, at any instant, provide an equal and opposite m.m.f. to that of the cable conductors. If these reverse currents are confined to the sheath and armouring, no external magnetic field will result, and the system would have approximately the same conductor inductance, capacitance and natural impedance as a 3-core cable.

When referring to d.c. transmission, with sheath return, the authors point out that return currents leave an uninsulated sheath and choose the much lower resistance path provided by the sea. It is probable that induced reverse currents do the same in the a.c. case. The resulting magnetic field would be difficult to assess, but is of considerable academic interest. It would appear certain, at any rate, that the sheath and armouring provide only partial shielding against external magnetic fields.

If this is so, the inductance per conductor per mile, the characteristic impedance and the wavelength constant could be much greater than might be expected for a cable system. In certain assumptions are made (such as the maximum stress in the dielectric) the limiting values of characteristic impedance and wavelength constant can be calculated for any of the cables referred to in the paper. On the assumption for the 100 MW 'notional' cable, described in Table 3, of a 37-strand conductor and a maximum dielectric stress of 100 kV/cm, the following results are obtained:

#### Inductance per conductor per mile

No external field	..	..	..	0.33 mH
Unrestricted external field	..	..	..	4.37 mH
Capacitance per conductor per mile	..	..	..	0.288 $\mu$ F

#### Characteristic impedance

No external field	..	..	..	52.5 ohms
Unrestricted field	..	..	..	122 ohms

#### Wavelength constant

No external field	..	..	..	0.23° per mile
Unrestricted field	..	..	..	0.6° per mile

#### Total phase shift on $Z_0$ load

No external field	..	..	..	6.0°
Unrestricted field	..	..	..	15.6°

#### Approximate phase shift on full load

No external field	..	..	..	1.9°
Unrestricted field	..	..	..	10.5°

I should be interested to know where, in fact, the values of characteristic impedance and wavelength constant lie inside the very wide range indicated.

If the external field is appreciable in the a.c. case, and since it is envisaged that the cables may some day be used for d.c. transmission, it might be an economical proposition to use an insulated sheath on the cables.

The load impedance of a 132 kV cable supplying 100 MW is 174 ohms, so that it is apparently intended to operate the cable at a fraction of its characteristic load impedance. This may be necessary from stability considerations, in view of the rather tenuous connection to the two systems, and more especially the frequency of one, at least, of those systems is not stabilized. However, if the wavelength constant is near the lower value estimated above, it is difficult to see how the cable could materially affect the stability of the connection. It is therefore surprising that a lower operating voltage (such as the 70 kV available on the French side) was not considered, in conjunction of course, with a conductor of larger cross-sectional area.

### MERSEY AND NORTH WALES CENTRE, AT CHESTER, 7TH FEBRUARY, 1955

**Mr. P. d'E. Stowell:** Evidence in Section 13.1 shows that there is a not inconsiderable amount of diversity between the loads on the two systems. Since this appears to be due in the main to a factor that could be removed almost overnight by Act of Parliament in either of the two countries, it must make one some-

what apprehensive as to whether this factor can be relied upon to continue for the number of years normally necessary to make such a scheme economic. I refer, of course, to the fact that France has summer time all through the winter, presumably a war-time introduction, as it was in Britain. Since the paper was written

two more winters have been completed, and it would be interesting to know the figures for 1953-54 and 1954-55 to correspond to those given in the paper.

The authors conclude that the prudent course would appear to be an initial installation of the minimum capacity consistent with reliable operation, but planned with a view to increasing the capacity when service experience has proved its worth. I suppose this is so, but as a powerful interconnection is undoubtedly desirable operationally, and as the cost of interconnection capacity per kilowatt is a very important economic factor and the paper shows it to be much lower for the 400 MW 275 kV scheme than for the 100 MW 132 kV scheme, it would be a pity to spend money on laying 132 kV cables to be superseded later if, in fact, a 275 kV scheme is economic, practicable and operationally desirable, and I think the paper suggests that it is.

What is the relationship between the peak voltage at which a given cable can be operated satisfactorily and frequency down to, say, 1 c/s and below, and eventually down to zero or direct current? I would presume that there is a progressive increase in permissible peak voltage with decrease in frequency rather than a sudden jump at zero frequency. If so, would there not be some point in going down to a very low frequency, which might not be any more difficult to convert at the ends than zero frequency? I admit that transmission capacity would apparently still be only 70% of that at zero frequency by reason of the factor representing the ratio of r.m.s. to peak values on sine waves, but if very low frequency gave an improvement of the order of 1.9, i.e. the ratio of direct to 50 c/s peak voltage, it might be worth while offsetting the potential extra 40%, i.e. the ratio of peak to r.m.s. voltage, against other problems that arise at zero frequency, e.g. corrosion, about which there is general concern and of which the authors have not lost sight in the paper.

**Mr. E. L. Davey:** For a 138 kV submarine-cable project laid at 600 ft it has been found necessary to use a tightly sheathed cable to avoid the deformation under high external pressures which occurs with a loosely sheathed cable. To provide the longitudinal gas feed passage a hollow conductor is used. This design is superior mechanically, for when a high tensile load is applied to the armour the sheath is not compressed by the resulting radial pressure and the movement of the sheath relative to the conductor is greatly reduced. Furthermore the longitudinal gas-flow resistance is a good deal less than that of the loosely sheathed cable.

Another point relates to the armour, which is applied in such a direction that it loosens when the cable is coiled into the hold of the ship. In order to produce a mechanically balanced cable, the reinforcement is applied with opposite lay to the armour. When a single wire-armoured cable is laid in deep water the armour tends to untwist in proportion to the tension. The latter is a maximum at the ship end and a minimum at the sea bed, and since the two ends are fixed as regards twisting, the untwisting at the top end of the cable must be balanced by a twisting of the cable at the sea bed. If the tension is released from the cable in this condition a kink may be formed in the cable on the sea bed. The reinforcement tends to prevent this action if it is applied of opposite lay to the armour, which is applied at as long a lay as possible.

From a cable designer's point of view it is very desirable that the diameter of the eye of the cable hold in cable-laying ships

shall be as large as possible, in order to prevent straining the reinforcement during coiling, when the armour is loosened and the reinforcement is tightened.

For repair of submarine cables it is necessary to have some means of identifying the defective cable before it is grappled. Recently we grappled a cable in 60 ft of water, and the lead and rubber anti-corrosion sheaths were intact when the cable was raised, but the dielectric was ruined.

It should be noted that, owing to the long length of the submarine cable, the impulse duty is a good deal lower than those of shorter land cables. An impulse wave entering the cable is refracted to a low value, and because of the time taken to travel to and fro along the cable, the reflections do not build up to as high a peak value. This factor is of great importance with regard to the impulse withstand level allowable for the cable and flexible joint, as a submarine cable must be subjected to much more severe handling than a land cable.

**Mr. P. M. Hollingsworth:** The hazard of joint weaknesses—one of the most serious that can arise—can be reduced to acceptable proportions, but clearly the ideal to aim at is to eliminate joints altogether, apart from repair units. This entails making cable in continuous lengths of many miles, and it may not always be appreciated that the only type of construction which permits the manufacture of high-voltage cable in this way is the pre-impregnated insulation embodied in the gas-filled type of cable.

Not everyone would agree that this type of cable is necessarily the best and most efficient for land transmission, but it has two important advantages for submarine work in addition to being suitable for continuous manufacture. One of these is service experience going back over many years, from which its electrical characteristics under high-voltage operating conditions are well known; the other is that it provides for easy gas flow and saturation with gas—a highly important matter in a long length of feeder with no intermediate feeding points. This feature is assisted still further in the design of cable for the Vancouver project by introduction of the hollow conductor. Moreover, in view of its record of service, it is hardly presumptuous to claim that the gas-filled cable will be free from deterioration at the design stresses mentioned in the paper.

**Mr. T. R. Y. Grahame:** It would appear that, for a certain portion of the route, the cable would be laid over the Varne Bank at rather shallow depth and subject to damage. Is it intended to risk this hazard or will a channel be dredged across the bank? It would also appear that the cable will cross Post Office circuits; has any method of protecting the latter been considered? It is rather surprising that the oil-filled cable was not more favourably considered, since the operating pressure would obviously be considerably lower and limited only to 100 lb minimum by the water pressure. This lower pressure would obviously give less chance of leaks, but the advantage may be outweighed by the larger charging current.

In Table 6 the dielectric losses for a comparative French cable are very much higher than for the British product, presumably on the same specification. It would be interesting to know why this is so.

In the compression-type cable where pilot wires are provided, are these to operate a pressure switch in the joints for gas-leakage location, or for protection purposes?

**Mr. J. A. Spence** also contributed to the discussion at Chester.

#### NORTH-EASTERN CENTRE, AT NEWCASTLE UPON TYNE, 14TH FEBRUARY, 1955

**Mr. R. G. Sell:** The new Canadian 138 kV 120 MVA submarine power cable, designed to carry surplus hydro-electric power from the mainland of British Columbia to meet the growing needs of

Vancouver Island, will consist initially of four single-core armoured cables of the pre-impregnated gas-filled type. The route consists of a main crossing of some 15 miles across the

Strait of Georgia and a secondary crossing of some  $3\frac{1}{2}$  miles across Trincomali Channel, with intermediate and final links by overhead line. The maximum depth of about 100 fathoms is nearly three times that of the English Channel and corresponds approximately to an external pressure of 280 lb/in<sup>2</sup>. Consequently, the internal gas pressure of the cable has been increased to 300 lb/in<sup>2</sup> to maintain a positive internal pressure differential to all depths. The high external water pressure has led to a significant change of design—a hollow conductor with a 12 mm duct being used to provide a gas path along the length of the cable, because it was felt that the sheath clearance originally proposed for the cross-Channel cable, following land-cable practice, might lead to difficulties such as creasing of the sheath. In other respects the cable is similar to the design proposed for the cross-Channel project, except that galvanised-steel armour wire has been substituted for aluminium-alloy wire.

The differences in cable design and installation conditions, with particular reference to depth of water, led to the conclusion that it would be prudent to carry out further sea trials, which were accordingly arranged during the latter part of 1954. For these trials H.M.T.S. *Alert* was used in Scottish waters where the appropriate depth of about 100 fathoms was to be found. As a result of preliminary trials some changes were made to the proposed cable design, chiefly with the object of limiting the tendency of the cable to twist under the laying tension of some 5–6 tons in this depth of water. The revisions to the cable design were subsequently tested at sea with extremely satisfactory results, thus establishing not only that a heavy power cable of this type can be laid successfully in this depth of water but also that it can be recovered by grappling, which would be essential if a repair had to be carried out.

Reassured by these results, manufacture of some 3 500 tons of cable required for this job will begin very soon in readiness for its installation by cable ship in 1956. This cable link will not be the longest submarine power cable, nor will it be the installation in deepest water, but it is believed that it will carry more power than any other submarine cable operating at this voltage. It is certainly unique in the method of manufacture, which is designed so that the longest lengths of over 15 miles can be manufactured continuously. By this means it is hoped to obviate troubles arising from joints in the conductor.

It is apparent from the authors' conclusions that important submarine power-cable links must operate at voltages of 132 kV or higher, and in this respect the proposed installation will provide valuable operational experience. The additional sea trials referred to have given the British cable makers concerned confidence in the successful execution of the Vancouver project, using gas-pressure cable. Might one conclude that, in some ways, the cross-Channel cable should present less difficult problems?

**Mr. C. H. Lackey:** My main worry about a submarine cable link is its reliability, having regard both to the technical difficulties and the special hazards in that part of the sea. I agree that we can only guess at the hazards, but it would seem that there is a real risk of the cable being fouled by ships' anchors, especially in bad weather conditions, and it is, of course, just then that it would be most difficult to effect a repair. From this aspect a 3-core cable would be better than three single cores. With single-core cables 1 000 yd apart the sheath losses as given by the authors are 170% of the core loss. The sheath loss of a 3-core cable would be very small in comparison, and presumably the cross-section of the cores could be reduced on this account, making the cable easier to handle, although it would, of course, be more difficult to handle than single-core cable and would take longer to repair. Is a 3-core cable impracticable?

Associated with continuity of supply is the method of earthing the system. Have the authors considered the possibility of

using transformers at each end of the cable and operating the cable link with arc-suppression-coil earthing? This would be of special value with single-core cables and enable the link to be operated for some hours or even days with a fault on one phase. This would be specially useful in bad weather or while waiting for a cable ship to get under way.

I am glad to see the reference to the possibility of a d.c. link. If three concentric cables were used instead of the three single-core cables there would be considerable improvement in continuity, especially if it were possible to switch each cable separately. This, of course, means high-voltage d.c. circuit-breakers and suitable discriminating protection, neither of which exist at the present time.

Since no shunt reactors are proposed, what do the authors consider to be a practical figure for the 50 c/s voltage at the open end of the cable? Assuming 132 kV for the link, the transformer would be at the French end, and the worst conditions for voltages would arise at the British end with that end open. The source impedance on the French side should therefore be as low as possible in order to keep the British end voltage down. The predominating impedance will probably be that of the transformer, and it may therefore be desirable to design this with a specially low leakage reactance. I agree that shunt reactors should be avoided if possible, but the voltage-rise problem will have to be watched carefully.

**Mr. F. H. Birch:** The authors deduce that the installation of the cable would lead to substantial savings in capital investment on generating plant in both countries. It appears that the estimated savings given in Section 13.2 assume that the service reliability of the cable will be as good at times of maximum demand as the average reliability of generating plant in the two countries. In Britain, the proportion of generating plant unserviceable at the time of maximum demand has averaged about 12% over the last few winters. In other words, a particular machine is likely to be out of service at the time of maximum demand once every eight years. Since the maximum demands of the supply systems in both countries occur in the winter, when weather conditions in the Channel are at their worst, it seems doubtful whether such a degree of reliability would be obtained, even with a spare cable. Not only would damage by ships' anchors and fishing gear be most likely to occur at this time of the year, but also the winter gales might delay repairs considerably.

Do the costs given in Table 3 allow for a faulty cable to be switched out and a spare cable switched in at sufficient speed to prevent the systems in the two countries falling out of step? It would seem that the provision of high-speed single-phase auto-reclosing circuit-breakers for the spare cable would make the best use of the capital invested in it.

What type of protective gear is envisaged for the cables? The cable impedance might well be too low to allow plain distance protection to function satisfactorily, and the cables would probably offer too high an attenuation to permit the economic use of a carrier-current channel. I believe that a v.h.f. radio link would offer the most satisfactory and economic channel of communication between the ends of the cables, would enable excellent protection to be given and would provide useful channels for telephone communication.

In view of the high cost of repairing damage to the cables there seems to be a good case for considering the installation of radar equipment to detect ships approaching the cables and radio equipment to warn ships not to drop anchor until clear of the cable route. It is realized that the two countries would have no rights under international law to impose restrictions on shipping outside their territorial waters, but it would appear that hazards to the cables could be considerably reduced by tactful warning of shipping.

In Table 3 the cost of land connections on the French side amounts to 40% of the total cost of the 275 kV scheme but less than 18% of the total cost of the 132 kV scheme. What is the reason for this marked difference?

**Mr. R. A. Hore:** Whether or not a cable can be laid depends on how much money and effort we are prepared to expend; the primary consideration of this project—to which the paper devotes relatively little space—is whether, if a cable were laid, the link could be operated satisfactorily. The steady-state power limit of the link appears to be in excess of 300 MW, leaving a reasonable stability margin (steady-state and transient) when operating with a power transfer of 100 MW. It also appears that no particularly high-speed clearance of faults will be necessary to maintain transient stability, owing to the large inertias and consequently slow swings of the two systems.

On the other hand, oscillations of power in the tie may be troublesome. Such oscillations would be caused by rapid changes of load on either system (rapid, that is to say, compared with the natural period of oscillation of the combined system). If, for example, there is a sudden load increase of 50 MW on the English system the initial distribution of this load will be taken up in accordance with the synchronizing power coefficients of the English machines and of the tie. The final distribution of power pick-up will be in accordance with the governor droops of all machines on both systems (in view of the long period of natural oscillation we may in this case neglect the intermediate power distribution conditions). The difference in tie-line power between the initial and final pick-ups will result in oscillations of tie-line power, and if the governor droops are so arranged to give minimum oscillations for both a 50 MW increase on the English side and a 25 MW increase on the French side, the magnitude of the tie-line power oscillation will be between  $\pm 15$  and  $\pm 20$  MW for either case. The oscillation may be expected to include beat frequencies of as low as 2 c/m, and from calculations made the time-constant of decay of these oscillations will be very long. In view of the continuous changes of load on both systems it seems likely that a continuous oscillation of at least  $\pm 20$  MW may be expected on the tie. I should like the authors' views on these points and to have their assurance that these oscillations will be stable, not self-excited.

This oscillation of tie-line power is well known. The oscillation

magnitude is independent of the load carried by tie line, and since the latter is small in this case, the oscillation magnitude as a proportion of the tie-line steady power is larger than normal. The ratio is further increased because the magnitude of the oscillation is absolutely larger in this case, owing to the small synchronizing coefficient of the tie line.

It is clear that refinement in the control of frequency of both systems will not reduce the oscillation magnitudes; the only possibility of doing this appears to be control of the tie-line power angle, which must be very stable and very accurate. It therefore appears that even more extensive control equipment will be required for the satisfactory operation of the tie than the authors indicate, and I should like further details of the control proposed and, in particular, of how the cost of providing this control affects the overall economics of the scheme.

Can the authors give, as a result of their tests, a more accurate estimate of the sudden load changes which occur on the two systems, which I have assumed to be 50 and 25 MW?

**Mr. G. Lyon (communicated):** It may be useful to distinguish between the variation of load and frequency reported in the paper and variation of load with frequency and voltage reported by earlier authors giving quite different values. In previous tests made in several countries the objective was to determine the shedding of load on generating stations due to the deliberate or involuntary reduction of voltage and/or frequency, and the results showed a slope of about 1 : 1 of power with either voltage or frequency. The tests reported in the paper, on the other hand, show the variation in power in a tie between a composite system and an external reference point or 'infinite bus,' and the slope of the power/frequency change is of the order 10 : 1. The large difference is due to the action of the generators in the composite system under the control of their governors.

It has been stated that further tests were in prospect. Can the authors report the results of these tests? I have noted that the results\* of some similar tests in France have been published during 1954.

**Dr. B. Salvage** also contributed to the discussion at Newcastle upon Tyne.

\* CHEVALIER, A., HOLLEVILLE, M., and PASSERIEUX, P.: 'A Study and Predetermination of Power Fluctuations on an Interconnector between Two Networks,' *Bulletin de la Société Française des Electriciens*, 1954, 4, p. 401.

#### NORTH STAFFORDSHIRE SUB-CENTRE, AT STOKE-ON-TRENT, 11TH MARCH, 1955

**Mr. J. H. Pirie:** A cable which will resist the ingress of water when punctured is the solid type impregnated with a high-melting-point low-viscosity petroleum jelly. Although its a.c. performance is strictly limited, its d.c. performance is quite satisfactory, and I estimate that, with a  $0.25 \text{ in}^2$  conductor and 0.4 in radial insulation, three such cables will have a capacity

of 150 MW, i.e. 50% greater than the proposed a.c. design. The cables will also be thinner and lighter, since the increased thickness of lead sheath proposed for the gas-filled cable is not required.

**Messrs. H. A. P. Caddell, L. Goodall and J. H. C. Peters** also contributed to the discussion at Stoke-on-Trent.

#### NORTH MIDLAND CENTRE, AT YORK, 5TH APRIL, 1955

**Mr. H. C. Ogden:** At present the French operate fully in parallel with Belgium and Germany, but only with sections of the Saar, Switzerland, Italy and Spain, who themselves, however, are interconnected with Holland, Austria and Portugal. It is quite feasible that these countries will be fully in parallel with France by the time the cable is ready, and this will mean a continental load of some 35 000 MW while Great Britain would have some 25 000 MW. On one occasion in 1951 we had the north and south of this country coupled through a single 90 MVA line, the groups being of some 3 700 MW and 5 000 MW respectively. This line tripped on over-current at a time of rapidly changing load on the system. Control, of course, was

manual and operating techniques have since improved, but the difficulty is obvious.

The tests in June, 1953, were repeated in December, 1953, and March, 1954. The paper mentions that the frequency differences between Great Britain and France were to be recorded, and this was done in May, 1954. All control engineers and station staffs were specially briefed, and the arrangements were made in an endeavour to approximate to automatic control. Histograms have been prepared showing the British frequency on manual control, the British frequency simulating automatic control and the French frequency, which has automatic control. With ordinary manual control the histogram is flat-topped, as normally

no action will be taken on very small changes of frequency, but the graphs showed that with special arrangements manual control can be even better than automatic control. The special arrangements would, however, be impracticable on a day-to-day basis, and hence it would appear that manual control would not be satisfactory as a general rule but that automatic control should be.

The various tests which have been carried out show that, at present, a flow over the link of some 1 000 MW would correspond to a 1 c/s difference of frequency if the link were open. This figure is at the time of maximum load. By the time the cable could actually be in operation the relationship would have risen to 3 500 MW per cycle. It appears from the calculations which have been made that a cable of 100 MW capacity would not be large enough, although cable heating experiments using thermal images are in progress to gain further information.

**Mr. A. M. Morgan:** Details have recently been published of a process for jointing compression cables so that the joint is of equal diameter as the cable and also has the same mechanical and electrical properties. The procedure is somewhat more elaborate than would be used for land cables, but the complications are not so involved as those introduced when manufacturing a complete 26-mile length of cable. There appears, therefore, to be a very good case for using compression cable.

The extension of the fourth cable for use in an experimental d.c. transmission scheme would receive universal support. Should the authorities decide to go ahead with this, would they propose to install only one type of cable, since this seems to be inferred from the test programme described in the paper?

At present the d.c. installation at Gotland uses solid-type cable. There is no evidence on the behaviour of oil-filled, gas-pressure or compression cable in a d.c. installation. An expensive installation should give the maximum amount of information, and should the test programme show that both pre-impregnated gas-pressure cable and compression cable have the requisite mechanical and electrical properties for a.c. operation, it would be wise to install two cores of each of these types. Here I am not sure whether the difference in capacity of the two types

arising from the differing electrical design stress and permittivities of the dielectrics would complicate the operational problems.

**Mr. H. S. Moody:** Has consideration been given to the use of aluminium conductors in the cables in place of copper conductors? Besides providing a cheaper cable, this would have the great advantage of producing a lighter cable. The use of hollow-core conductors would appear to be advantageous to permit the rapid charging and discharging of gas pressure.

The design of the rigid type of joint for rapid repairs at sea is obviously a matter of much importance, and a modification of the stop joint used on oil-filled installations might provide the basis for suitable design. Such joints are partly made in the factory.

At first reading, the fact that no less than four cable faults developed on these comparatively small-scale trials appears somewhat alarming. The author explains these failures to some extent, but they seem to indicate that further trials will be necessary before the full-scale installation is commenced.

It would appear almost inevitable that the ultimate solution to the problems associated with a continental link of this kind will be the use of direct current, in which case presumably two 200 kV single-core cables will provide a 400 kV link. I am unable to assess the importance of the 4° deflection of ships' compasses when crossing over a d.c. circuit, but if this is important I suggest that consideration could be given to the use of a concentric type of cable. Such a cable would be heavy, including as it would both cores within one sheath, but the outer conductor would have to be only lightly insulated from the sheath, the cable would be extremely robust and there would be no external magnetic field. I am, in fact, rather surprised to note that consideration has been given to an earth-return d.c. system on such a comparatively short crossing. I should have thought that the cost of the additional insulated core would be well worth while in comparison with the serious dangers which must inevitably be encountered on any form of earth-return system.

**Messrs. R. G. Sell and W. J. A. Painter** also contributed to the discussion at York.

## DISCUSSION ON 'THE ADHESION OF ELECTRIC LOCOMOTIVES'\*

NORTH-WESTERN CENTRE, AT MANCHESTER, 1ST NOVEMBER, 1955

**Mr. F. Whyman:** So far as I know, no difficulties have been experienced in operating 750 tons by a single locomotive over gradients of 1 in 100 or in operating 750 tons over gradients of 1 in 40 by two locomotives. Considerable thought over almost the last 20 years has been given to this problem, and difficulties have been predicted if train loads above these are attempted, particularly bearing in mind that, with the method of train-weight computation used, an 850-ton train can actually vary between the limits of 750 and 950 tons.

In general I would agree with the use of a limited maximum current for starting 820-ton trains with two locomotives on the 1 in 40 gradients, provided this were laid down as a nominal weight of 750 tons which would occasionally rise to nearly 820 tons.

Considerably more is known by locomotive designers about adhesion than is generally realized, and when the track condition and locomotive design are known, the loads which the locomotive can start and haul over a given profile can be very accurately predicted. The load to which I refer is that which can be started under any of the many variable rail and weather conditions, and which can so often be considerably exceeded under good or normal conditions. It is, however, of little use to specify a train weight which can usually be hauled only to find that adverse rail or weather conditions cause the train to be stalled.

The author refers to the conflicting published tests on adhesion, and in general it can be said that these do not really represent fundamental adhesion information but really the adhesion characteristics of particular locomotives under certain track conditions which are rarely, if ever, recorded. Under these conditions, correlation of the results is impossible and the data are not of great use; for example, Figs. 2-6, which if all the facts were known could no doubt readily be reconciled and in addition provide very useful information.

Relative to Dr. Andrews's tests much the same criticism applies. Curves of measured adhesion are given where the actual pressure between wheels can only be guessed. The most potent factor in varying this pressure is negotiation of rail joints where the wheel often falls 2 in and inertia effects are very important, particularly with increasing speed. The next is the oscillation of the bogie on its main springs, periodically loading and unloading an axle. Again the use of series resistance to regulate the current of the motor under test is a very predisposing cause of wheel slip, as an incipient slip has no chance of recovery. None of these factors is taken care of in the assumed wheel and rail pressure, and in addition the method of allowing for weight transfer is incorrect.

Whilst Appendix 11 shows that, with a free two-axle bogie, the bogie tractive effort multiplied by the pivot height and divided by the wheel base is transferred from one axle to the other under tractive-effort conditions, the locomotives in question are quite different in that the bogies are connected by an 18 in articulating joint which radically alters the weight-transfer characteristics of the bogie, so that the formula mentioned above for weight transfer does not apply and a substantial correction is necessary. It is not surprising that there are wide variations in the results.

Generally it can be said that to obtain the best adhesion results the actual pressure between rail and wheel must be maintained as constant as possible under all static and dynamic conditions. This, however, particularly from the dynamic aspect on poor track, can be done only imperfectly; and the next most important feature is that, when a momentary slip is produced, conditions favouring its immediate suppression should be provided, such as a condition where tractive effort falls away rapidly with increase in speed or a momentary brake application to that particular axle is made. The first of these points is made by Royer.<sup>8</sup>

The next condition is that the adhesion of a thoroughly wet rail, i.e. with rain continuously falling on it, is in general only little inferior to that of a dry rail, and it is fairly well established that the very low adhesions so frequently experienced on rail which is only slightly wet are due to the rapid spreading of very thin oil films which are so readily washed away by rain and cannot, of course, spread under dry conditions.

Again, it is fairly well agreed that the true coefficient of friction between wheel and rail is not affected by speed, provided that slipping has not taken place, and that the apparent reduction in adhesion with increasing speed is usually due to imperfections in the track and riding qualities of the vehicle.

In the latter connection the varying spring link tensions shown on curves such as Fig. 33 are, I think, in the case of the Manchester-Sheffield locomotives, due almost entirely to resonance caused by the tension in the articulating joint link under tractive-effort conditions, which explains why there are such great variations in the spring-link tension under those conditions.

The author has not referred to the apparently high adhesion performance obtained with 50 c/s traction with rectifier-fed d.c. motors in France, but a reference to the points I have made above readily covers the apparently wide discrepancies between performance on the Valenciennes-Thionville and the Manchester-Sheffield lines.

When watching trains being started and hauled on the Manchester-Sheffield line, I have frequently seen rail joints rise and fall 2 in as the train passes over them; but when watching similar tests recently on the Valenciennes-Thionville railway, it was impossible to detect by eye any rise and fall, and it was interesting to see that when a wheel slipped it slipped at only a very low creep speed and the steeply falling tractive effort with rise in axle speed readily permitted the suppression of slipping.

Finally, relative to the author's ingenious method of measuring the actual spring force on an axlebox by measuring electrically the tension in the spring hanger, I think the results are very open to suspicion, as the considerable vertical friction of the axlebox guides is neglected. For light running conditions this frictional force can be considered reasonably low, but for higher tractive efforts it increases very rapidly, and it is for these latter that the actual force between wheel and rail is required.

**Mr. J. K. Lord:** Throughout the paper, reference has been made to an unknown 'rail factor', and I am wondering if rail wave formation, or what is perhaps more commonly known as Zimmerman's curve, is the unknown factor.

The 'down' track on Worsborough branch, where the tests were made, is laid with two types of rail, i.e. bull head and flat bottom, the bull head from Wentworth to Sovereign Bridge and the

\* ANDREWS, H. I.: Paper No. 1797, April, 1955 (see 102 A, p. 785).

flat bottom from Sovereign Bridge to Silkstone Tunnel. It is on the latter section that most of the slipping occurs. Did the author give any consideration to this?

About the most serious maintenance problem we have had to face with the  $B_0+B_0$  locomotives has been with the traction motor resistances through overheating. I am a little apprehensive, therefore, regarding the suggested driving methods with limitation of current and Vernier notching. Can the author say whether overheating will be more or less if these methods are adopted?

Responsibility for the slipping which occurred during the tests was placed on the drivers because of their alleged lack of co-ordination, although arrangements had been made for a technical assistant to travel with each driver to relay instructions simultaneously from the test controller situated in the dynamometer car.

It is, of course, most important that the train and banking engines should be worked as one, and I would mention that we have experimented with telecommunication equipment using the overhead line as a carrier. Trains of 850 tons were worked up the 1 in 40 gradient; also, starts were made under good and bad conditions, without slip. The tests have proved so successful, not only for these reasons but for working trains during fog, that authorization has been obtained to equip a number of locomotives with telecommunication equipment.

**Mr. H. Charnley:** I think an improved control system would improve adhesion. Since the author has shown that rate of change of tractive effort had little effect on adhesion, he has assumed quite wrongly that the control system has little effect on adhesion.

Consider a motor of such a type and feed system that, when it is rotating at a speed corresponding to 5 m.p.h., the tractive effort is 10 000 lb. If, however, owing to tendency to slip, the angular speed increased 1% and the tractive effort dropped from 10 000 to 1 000 lb, there would, of course, be no tendency to slip.

With several series motors in series, together with starting resistances, the counter-e.m.f. of one particular motor which slips has little effect on the current through the motor; therefore, if a wheel has a small slip, the motor current is not much reduced and incipient slips become real ones.

I think that the somewhat large diversity of the adhesion figures shown in Fig. 17 is largely due to track irregularities. With the ideal type of locomotive these irregularities would cause only incipient slips, but on the locomotives under discussion, these incipient slips develop into real slips giving the low service adhesion of 0.16.

Since more tests are to be carried out, I would like to see the one motor fed from a level compounded generator carried on the locomotive and the traction-motor field separately excited. From my own studies and tests, I am sure that Fig. 17 would show much more consistency, and the values of adhesion would fall near the 0.4 mark. Experience on the Valenciennes-Thionville line indicates that values of 0.3 can be regularly obtained in service.

**Mr. W. Train Gray:** As control engineers we do our best to prevent wheel slip by smooth notching and weight transference, and we can fit slip indicators to warn the driver when it occurs.

There can be no doubt that the slope of the torque/speed characteristic curves of the motor during acceleration have some effect in re-establishing adhesion with varying rail conditions, and some claims are made that motors fed from a variable-voltage source, such as motor-generator or rectifier locomotives, are better in this respect than those using resistors. The control designer, however, is in a quandary. The flatter the motor curves are made to assist re-establishment of slip, the more notches he must put in to limit the tractive effort peaks and so

prevent slip starting. It seems to be necessary to have some idea as to how the coefficient varies with wheel slip, particularly in the earlier part of the curve. Certain tests have been made, but no information is available, to my knowledge, as to how the coefficient varies between, say, 0 and 5 m.p.h. slipping speed. If we knew this we might be able to assess more accurately what benefit is likely to result, because some sweeping claims are being made.

In a recent paper published in the *General Electric Review*, reference is made to tests carried out in the United States on adhesion. These indicate that oil films of molecular thickness will destroy adhesion. I should like to have the author's view on these tests.

**Mr. G. R. Higgs:** The apparent contradictions of Metzkw's results probably derive in part from the fact that they relate to a non-driving wheel-axle set of low inertia on which rail pressure variations would be less than with the massive wheel-axle sets of the locomotives on which the other results are taken.

In the speculation on the reasons for the superiority in adhesion of coupled wheels over individually driven wheels, there is no reference to the fact that, when running over patchy rail, slipping occurs on the individual-drive locomotive when any one wheel set encounters a slippery patch, whereas in the coupled-axle case all wheels must simultaneously reach slippery patches—a much less probable event.

**Mr. A. B. Washington:** All the coefficient points and curves shown in the paper seem to be based on an axle load measured statically, with a correction calculated for the motor torque and draw-bar pull. The paper then proceeds to show that another large variant exists on these locomotives, namely the transitory oscillations of the bogies initiated mostly from the track, the effect being to cause momentary reductions in the axle loads.

The reason for these large oscillations is not hard to find on examination of the mechanical design. No restraint has been introduced in the design to prevent the bogies from rocking vertically about their longitudinal centres, relative to the body. Some form of fore-and-aft control of the tilting would improve the adhesion, and I understand this is now being considered. The original, or prototype, locomotive built before the war was in this respect superior, since its mechanical design incorporated features which held the bogies fairly rigidly against fore-and-aft tilting.

The train weights mentioned throughout the paper were of course accurately assessed, but I think the critical nature of train weights when working at the maximum adhesion on heavy-grade sections should be stressed; this applies equally to electric, steam or Diesel locomotives. In such conditions, some 90% of the total effort may be absorbed in overcoming the grade and train resistance, leaving only 10% for acceleration. If now the train weight is increased by only 10%, no effort will be available for acceleration and the train will stall. The method of assessing train weights originating from Wath did not always come within this rather narrow margin, being assessed by the guard on a quick rule-of-thumb basis. This was therefore another reason for the difficulty at times experienced on the Wath line. It is believed that more accurate assessment of train weights is now made.

The author states that the weight transfer switch provided correction which was to some extent incomplete. While this was so, it should be noted that very little improvement in adhesion would have been obtained even if the compensation had been 100%. In any case it is possible, with the method adopted, to obtain 100% compensation only at one particular value of motor current.

**Dr. H. I. Andrews (in reply):** In the calculation of weight transfer, described in the Appendix, the line of action of the

draw-bar pull,  $2T$ , is assumed to be parallel to the level of the rail. In actuality, as pointed out by Mr. Whyman, this may be angularly displaced, particularly in the case of the articulated coupling between the two bogies. This displacement will be dependent, not only on the tractive effort exerted by the bogie itself, but also on the alignment of the track, the relative position of the tractive vehicle or bogie to which it is coupled, and the effort which is transmitted through its frame—factors which it is difficult or impossible to include in the calculation. Fortunately, however, the articulated connection has an effective length of 3 ft 7 in and is at buffer height, so that usually the effects of reasonable displacements of the bogie are small and may be neglected.

The information contained in the paper is based on actual measurement, and there is no evidence of any appreciable increase of adhesion with very wet rail. It is true that locomotives often appear to slip less frequently when heavy rain is falling, but presumably at such a time the drivers have already reduced the tractive efforts to suit wet conditions, and a possible explanation of less frequent slipping may lie in the restoring action obtainable from the characteristics of sliding friction on very wet rail.

The theory of self-correction of slipping by the use of motors whose tractive-effort characteristics decrease sharply with increase of speed was originally propounded by Royer,<sup>8</sup> and has since been much employed by the French Railways. Its use for design purposes is, however, entirely dependent upon the relationship between the motor characteristics and those of sliding friction. The nature of the latter is roughly indicated in Reference 8, but the values do not agree with those determined from experiment by Phlanz.<sup>5</sup> Therefore, as pointed out by Mr. Train Gray, it is desirable that these characteristics should be reliably determined for locomotive driving wheels in actual running as soon as possible, so that full advantage may be taken of Royer's suggestions.

The method of weighing the axle loading does not take into account the friction of the axle-box guides, but this difficulty can be almost completely avoided by ensuring that the horn-block faces are well lubricated throughout the test.

Mr. Lord raises the question of the heating of resistors, which is most important in connection with d.c. goods locomotives. The method of driving with limitation of current does require the

resistors to be in circuit for a longer time, but this is greatly compensated by the fact that the currents carried are lower, so that the total heating is not greatly increased. On test, when starting a train of 850 tons on the Wentworth Bank, it was found that the heating of the resistors was only 20% greater with the limitation of current method. Now, the whole purpose of this method is to ensure that the tractive effort is below the limit of minimum adhesion, so that, under reasonable circumstances, slipping should not occur, and, with one service stop on the bank, the normal heating will be increased by only 20%. This is not the case with the normal method, however, and since a single slip may easily result in the heating being increased by 100%, it is probable that resistor maintenance will be lower with the limitation-of-current method. Any means of co-ordinating the action of the drivers at the opposite ends of these long trains will also be of help in this connection.

A very interesting study of the conditions on the rail head has recently been made by Allen in America, and methods of cleaning the rails prior to the passage of the wheels are being investigated. Unfortunately, full particulars of this work are not yet available, and so far it has been impossible to confirm certain of these observations in this country. As Mr. Washington points out, efforts here have been mainly concentrated upon measuring the actual values of nominal adhesion available under ordinary service conditions, and upon finding explanations for the results obtained.

In considering the effects of different control characteristics, mentioned by Mr. Charnley, it is important to differentiate between true adhesion and the characteristics of sliding friction. The object of the work described in the paper has been to study the limiting values of natural adhesion upon the rail, and, if these values are not exceeded, slipping should generally be avoided. The question of the re-establishment of adhesion after slipping has once commenced is quite a different problem, and here the suggestions of Reference 8 are probably most valuable as offering a means of overcoming the effects of temporary variations of adhesion or axle loading. It is, however, of first importance to secure the maximum initial adhesion between the wheel and the rail, and, once this has been satisfactorily accomplished, the question of re-establishment of adhesion will probably become of less significance.

## WRITTEN DISCUSSION ON 'THE SOLUTION OF GAS IN OIL DURING TRANSFORMER FILLING'\*

**Mr. R. A. Grierson:** The author appears to make a strong case for  $\text{CO}_2$  filling of transformers preparatory to oil filling for impregnation purposes. Successful experience with air as the filling medium, at reduced pressure, followed by impregnation with degassed oil which is further circulated after filling in a circuit embracing transformer and degassing plant, leads to an inquiry whether the extra refinement of  $\text{CO}_2$  as the gas-filling medium is justified.

In Section 3 the final paragraph indicates that the variation of

solubility with temperature for air is practically zero. I ask whether this statement needs qualifying, for while it may be true for oil which already contains its proportion of dissolved air, the rate of resolution of air in degassed oil is materially greater at higher temperatures, a factor which is used in eliminating trapped air and is applicable irrespective of whether  $\text{CO}_2$  is used or not.

Since  $\text{CO}_2$  is heavier than air, the displacement of air trapped under horizontal surfaces such as that shown in Fig. 1(a) of the paper is surely no more assisted than in the case of oil; in fact it may be less so, owing to the greater density of oil.

\* FRANKLIN, E. B.: Paper No. 1870 S, December, 1955 (see 102 A, p. 829).

Finally, CO<sub>2</sub> filling for dispatch or as an inert gas seal has been considered before, but the possibility of trouble from the freezing of reducing valves with consequent risk of damage to the main transformer tank has led to the adoption of nitrogen for this purpose with considerable success.

**Mr. J. R. Reed:** For many years it has been common practice in the cable industry to admit CO<sub>2</sub> to a cable after evacuation and to re-evaluate in order to ensure that the majority of the remanent gas is CO<sub>2</sub>. This then dissolves in the oil more readily than air, as the author rightly points out. Of recent years this practice has also been followed in the transformer industry with good results. However, it can give rise to an effect not found in cables where impregnation is carried out at a low pressure.

As the author says, in some cases it is impracticable to exhaust the transformer to a low pressure, and appreciable amounts of CO<sub>2</sub> are dissolved in the oil. If the acid value of the oil is measured, 1 mole CO<sub>2</sub> will combine under the conditions of the test with 2 mole potassium hydroxide to produce a high apparent acid value. Then, as the transformer is left in service and the CO<sub>2</sub> escapes from the oil through the breather, the apparent acid value falls.

This phenomena obviously gives an easy method of ensuring that the CO<sub>2</sub> content of the oil is such that supersaturation cannot occur at the operating temperature. The true acid value is readily measured by displacing the CO<sub>2</sub> by gently bubbling nitrogen through the oil.

I agree with the author that the use of CO<sub>2</sub> forms a valuable additional technique in the impregnation of transformers.

I cannot agree, however, that complete impregnation is achieved, in high-voltage transformers with thick insulation, even if the transformer is impregnated at a low remanent pressure of CO<sub>2</sub> with oil which has been treated by passing through a commercial degasser. The corona ignition voltage is certainly improved, but corona still occurs in the oil-impregnated insulation, as is evidenced by the fact that the corona ignition voltage increases with time of application of voltages in excess of it. This is found even though the test is carried out a week or more after the impregnation.

**Mr. J. Wainwright:** It is implied in the Introduction that transformers may fail at normal service voltage as a result of progressive damage to the insulation by discharges in gas-filled voids. Without in any way trying to minimize the seriousness of voids, I would suggest that this particular type of deterioration rarely occurs in transformers. It would therefore be useful if the author could give some statistics on the occurrence of this particular trouble.

Admittedly, when carrying out a post-mortem on a damaged winding, it is often impossible to find the original starting-point, but if voids are responsible it is more than likely that some evidence of discharge damage will be found elsewhere in the unit.

Has it been possible to show (say by means of radio influence or similar tests) that more complete impregnation is in fact obtained with the CO<sub>2</sub> method?

It is interesting to note that the use of CO<sub>2</sub> to facilitate impreg-

nation of paper with oil was mentioned as an established cable practice nearly 30 years ago by Emanuelli.\* Incidentally, an explanation is given of the formation of the typical void shown in Fig. 1(b) of the paper. Larger capillary forces are exerted in the pressboard than in the spaces, and the oil front in the pressboard advances so rapidly that the spaces are short-circuited and become voids.

To make the fullest use of the method, it seems that some experimental work is needed to determine the rate at which air can be withdrawn from the interior of large masses of pressboard, and also on the time taken for the comparatively dense gas, CO<sub>2</sub>, to infiltrate into the spaces. This point might have been mentioned in Section 8 of the paper.

The most impressive features of the method are that it may be possible to economize on tank weights and that impregnation on site may be improved, if CO<sub>2</sub> is used instead of air or nitrogen when transformers have to be shipped without oil.

It is unfortunate that the gas which is most satisfactory for the process should at the same time be one of the most expensive of those used industrially. A rough estimate of the cost for filling a large transformer is several hundred pounds. On the other hand, this figure is quite modest in comparison with the cost of the equipment and is well justified if increased reliability is gained.

**Mr. E. B. Franklin (in reply):** In reply to Mr. Grierson, the statement implying that the variation of solubility with temperature of air in transformer oil is small needs no qualifying, and further this applies even if the oil contains a portion of its dissolved air content. Because of diffusion, horizontal air layers could not exist for long. Even if some existed, their solution would be encouraged. From Section 3, the power required to absorb this air depends on  $(p - p'_g)k_b v_0$ . If the oil has dissolved a given quantity of gas  $v$  before reaching the air layer, the resulting value of  $p'_g$  will be lower if the volume  $v$  of gas already absorbed by the oil is CO<sub>2</sub> instead of air, and as a consequence the horizontal air layer will be more readily absorbed.

I am grateful for Mr. Reed's contribution. Of particular interest is the emphasis placed on the difficulty of complete impregnation of thick insulation, although all possible measures are taken to achieve this. I believe this is not generally so appreciated as it might be. It is interesting to have confirmation that the corona ignition voltage is improved by using CO<sub>2</sub> techniques.

Replying to Mr. Wainwright, although no statistics are available on breakdowns arising from voids, I see no reason for suggesting that transformers are particularly immune from such faults. Tests have shown that ionization can occur during service, if care is not taken during filling.† Radio influence is certainly reduced by using correct filling techniques. I agree that further experimental work is needed in line with Mr. Wainwright's suggestions.

In France the cost of gas for filling a 100 MVA 400/220 kV auto-transformer would be about £8.

\* EMANUELLI, L.: 'High Voltage Cables' (Chapman and Hall, 1929).

† BLANCHARDIE, R., and AFTALION, R.: 'Étude du seuil d'ionization dans les transformateurs', *Revue Générale de l'Électricité*, 1952, 11, p. 485.

## DISCUSSION ON 'AN EXAMINATION OF HIGH-VOLTAGE D.C. TESTING APPLIED TO LARGE STATOR WINDINGS'\*

Mr. A. W. W. Cameron (*Canada: communicated*): In the light of five years' experience of this type of test I would say that the authors' results show considerable promise, and I hope they will not rest discouraged with a technique which is providing others with increasing technical information and economic benefits.

When Fig. 16 is translated into resistance/voltage curves (see Fig. A) it can be seen that the authors' tests predicted

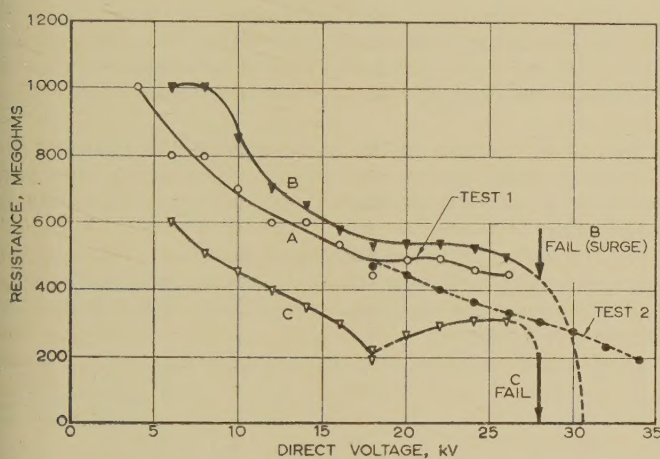


Fig. A

all the breakdowns in the aged stator. The usual manifestation is a curve with negative second derivative, a downward droop: this may be extrapolated, with continued curvature, to cut the voltage axis at the approximate breakdown voltage. In some cases there is a rise of resistance with increasing voltage, generally preceded by a downward droop: this supplies a warning of approach to breakdown, without means of assigning a definite figure; I incline to ascribe such behaviour to the drying-up of moisture by current in the fault path. The first test on phase C, culminating in breakdown at 28 kV, is typical of the latter phenomenon; experience suggests that the condition of the weakest spot may be indicated by the downward droop pointing to breakdown at about 19 kV, more accurately than by the eventual breakdown voltage, which reflects environmental conditions.

\* RUSHALL, R. T., and SIMONS, J. S.: Paper No. 1771 S, January 1955 (see 102 A, p. 565).

From 20 to 26 kV, phase B shows a droop which begins to suggest breakdown about 30 kV, so it is not surprising that it failed on the surge occasioned by phase-C breakdown. The first test on phase A is suspicious but inconclusive; the second, so far as it goes, hints at about 37 kV breakdown, and Fig. 17 shows phase A failing along with a portion of phase C at 36 kV, possibly on the surge. Fig. 17 shows a droop in the characteristic of phase C between 26 and 34 kV which unmistakably presages its 36 kV breakdown.

Such interpretations are greatly facilitated when the characteristic for the healthy state is known.<sup>†</sup>

The authors' test seems to have eliminated all defects with strength less than 26 kV r.m.s. The faults were at the core ends, a common location for delamination by ionization. This test aims purely at major insulation, and would not be expected to detect turn-to-turn defects.

The resistance/voltage test has been used for the maintenance of high-voltage stators in Ontario Hydro for the last four years. A 3-phase test occupies 2-4 hours. The resulting knowledge of the actual strengths of windings in service is a basis for the effective scheduling of repairs, and also for progressive revision of the specified strength towards the optimum for reliability and economy. As yet there have been neither unintentional breakdowns on test, nor service failures of insulation indicated as satisfactory by the test.

The advantages, compared with the uncertainties and the immediate repairs associated with purely withstand testing, are obvious. Resistance/voltage testing seems to be facilitated by cooling the winding to ambient temperature in humid air. It is difficult to reproduce the favourable field conditions in laboratory testing.

Messrs. R. T. Rushall and J. S. Simons (*in reply*): We are unable to agree with Mr. Cameron that our tests reliably predicted the stator breakdowns. There appears to be little justification for extrapolating the replotted curves of Fig. 16 as he suggests. The points of curve C, for example, show no downward trend at 26 kV, yet Mr. Cameron extrapolates this curve to predict failure at 28 kV.

The stator winding had failed in service as a result of internal corona in the slot portion of the winding, whereas all the d.c. test failures occurred in the end-windings. Our conclusion remains unaltered that the type of d.c. test described in our paper is incapable of detecting non-destructively the critically serious effects of ionization on the stator-coil slot insulation.

<sup>†</sup> ROSS, C. W., and CURDTS, E. B.: 'The Recognition of Possible Measurement Errors in D.C. Dielectric Testing in the Field', *Transactions of the American I.E.E.*, 1955, Paper No. 55-456.

## DISCUSSION ON 'THE OVERHAUL AND MAINTENANCE OF DIRECT-CURRENT TRACTION MOTORS'\*

**Mr. S. A. Vincze** (*New Zealand: communicated*): The figures given by the author for brush, commutator and tyre wear on rolling stock having axle-hung traction motors are most enlightening, and analysis of them reveals that: the brush wear on L.T.E. rolling stock amounts to 0.254–0.338 mm per thousand miles; the commutators need skimming by 0.01 in (0.254 mm) after 200 000 miles, which is equivalent to a wear of approximately 0.001 27 mm per thousand miles; and the motored wheels require turning every 12 months, or about every 50 000 miles.

The (brush wear)/(commutator wear) ratio is 200–252, indicating that the commutator is a rather reliable piece of equipment.

The figures for brush wear compare with continental data referring to d.c. locomotives, ranging between 0.161 and 0.322 mm per thousand miles.<sup>A, D, E</sup>

Since the erroneous belief that d.c. traction motors require less maintenance than their modern single-phase a.c. counterparts is still prevalent, the following figures might be of interest to traction engineers: the commutators of the totally-spring-borne 1 000 h.p. 16⅔ c/s single-phase traction motors of the 4 000 h.p. B<sub>0</sub>–B<sub>0</sub> locomotives of the notoriously difficult Bern–Lötschberg–Simplon Railway required no maintenance at all on completing 372 000 miles. The motor armatures had to be attended to for the first time after about 750 000–870 000 miles. The brush wear amounted to 0.145–0.274 mm per thousand miles and the flanges of the tyres had to be turned only after 300 000 miles.<sup>B</sup> Similar brush and commutator wears are reported from other single-phase a.c. railways.

The 16⅔ c/s single-phase traction motors of the Swedish State Railway exhibit brush wears ranging from 0.242 to 0.322 mm per thousand miles, and it is stated that the commutators last 4 million miles, equivalent to about 30 years.<sup>C</sup>

The German Federal Railways report brush wears of 0.193–0.435 mm per thousand miles on their 16⅔ c/s single-phase traction motors, depending on the age of the locomotive.<sup>D</sup>

Also it is reported that the maintenance of 1 500-volt d.c. locomotives and that of 16⅔ c/s single-phase a.c. locomotives are just about equal.<sup>E</sup>

**Mr. J. G. Bruce** (*in reply*): Although the figures provided by Mr. Vincze are most interesting and show that even commutator

motors on single-phase a.c. railways have improved to a state where they can no longer be considered the weaker brethren, they are not comparable in any way with figures given in the paper for the d.c. traction motors of multiple-unit rolling stock operating heavy-traffic frequent-stopping services.

The figures used in the paper were general averages as observed in the overhaul shops and were not the result of tests designed to prove any special point; it is therefore somewhat dangerous to particularize, for the following reasons:

(a) Service conditions for all traction motors are not the same. On some lines these are extremely arduous, with starting and stopping every ⅓ mile and some track curvatures as tight as 5 chains. On the other hand, some lines have much easier conditions.

(b) Similarly, some motors are as powerful as 300 h.p. and attached to trains composed mainly of trailers, while others are only 168 h.p. and are attached to trains composed mainly of motor-cars.

(c) The operation of a service of 40 trains/hour (i.e. a 1½ min frequency) makes it necessary to apply very stringent preventative maintenance procedures, especially regarding brush changing and commutator condition.

If we bear in mind these points the question of commutator wear can be examined. The figure given in the paper was that generally a skimming cut of 0.01 in after 200 000 miles' service was sufficient to restore the commutator. The actual commutator wear would, in general, be something less than this, and without special tests being conducted it could not be accurately assessed. As the commutator life is no longer the limiting feature of a d.c. traction motor, the prizes to be gained from any improvement in this wear are small.

On the other hand, the motor wheels on certain lines require turning every 50 000 miles, and any improvement in tyre wear produces considerable results. A figure of 300 000 miles between turning of wheels as stated for the B<sub>0</sub>–B<sub>0</sub> locomotive on the Bern–Lötschberg–Simplon Railway would be impossible on London Transport because of the track curvatures and the frequent starting and stopping.

On London Transport, too, commutators are expected to last 30–35 years, but in this time only some 2–2½ million miles have been covered.

This service is essentially one of stopping and starting and not of high speeds over considerably distances which the Swedish State locomotives must do to complete 4 million miles in 30 years.

(A) SEXAUER, O.: *Elektrische Bahnen*, 1951, March.

(B) MÜLLER, A. E., and BORGEAUD, G.: *ibid.*, 1953, 24, p. 162.

(C) ÖFVERHOLM, H.: *ibid.*, 1955, 26, p. 1.

(D) MANZ, G.: *ibid.*, 1952, 22, p. 301.

(E) LOREL, M.: *Bulletin of the International Railway Congress Association*, 1952, April, p. 74.

\* BRUCE, J. G.: Paper No. 1723, April, 1955 (see 102 A, p. 187).



# PROCEEDINGS OF THE INSTITUTION OF ELECTRICAL ENGINEERS

Part A. POWER ENGINEERING, APRIL 1956

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*Example.*—SMITH, J.: 'Overhead Transmission Systems', *Proceedings I.E.E.*, Paper No. 3001 S, December, 1954 (102 A, p. 1234).

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